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IN THIS ISSUE

Welding Tubes in Heat Exchangers 739
Principles for the Calculation of Welded Joints 743
Submerged Arc Welding of Chromium Bearing Steels . 752
Spot Welding Thin Aluminum 759
Resistance Welding in Jet Engine Manufacturing . . . 762
Welding Variables in Hard Facing 778
Welding of Heavy Sections 369-s
Effect of Temperature on Weld Metal Properties 388-s
Alloy Welds in Titanium 401-s
Effects of Gases in Welding Titanium Alloys 414-s
Weldments in Titanium-Manganese Sheet 417-s
30-Ft Diameter Welded Test Tank 422-s
Table of Contents 737
Index to Advertisers 828

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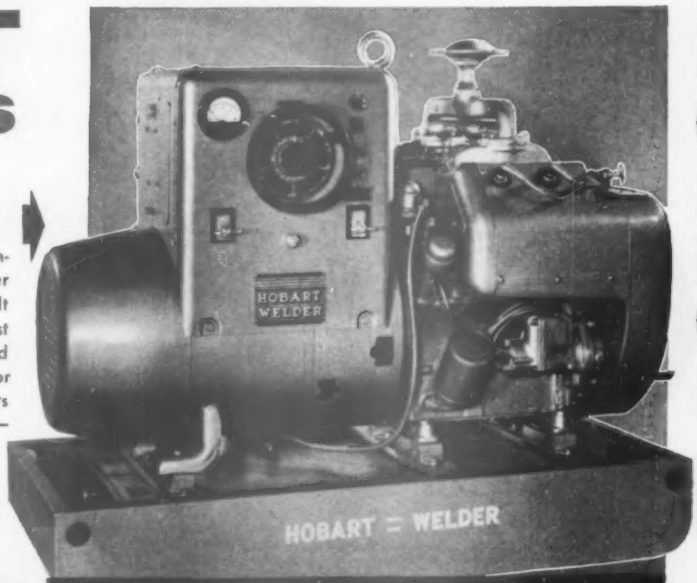

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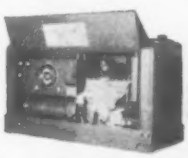
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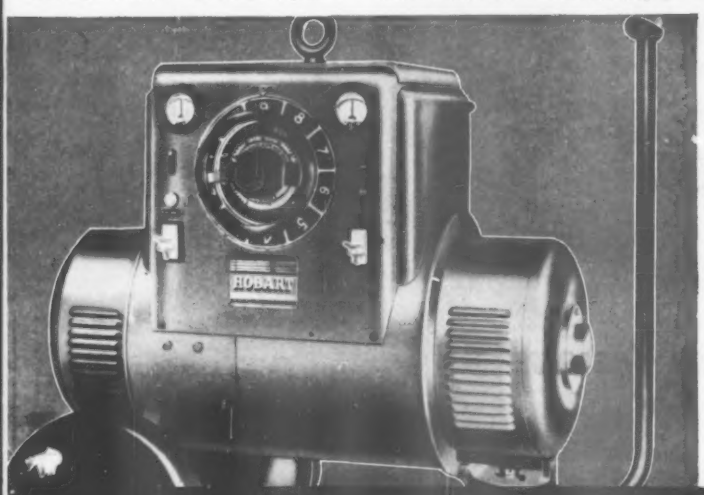

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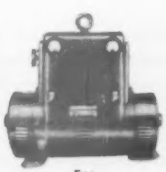
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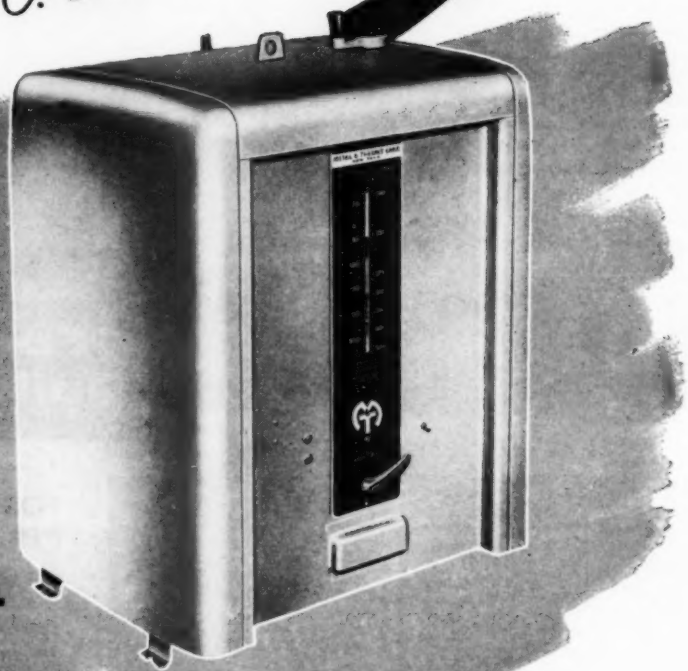

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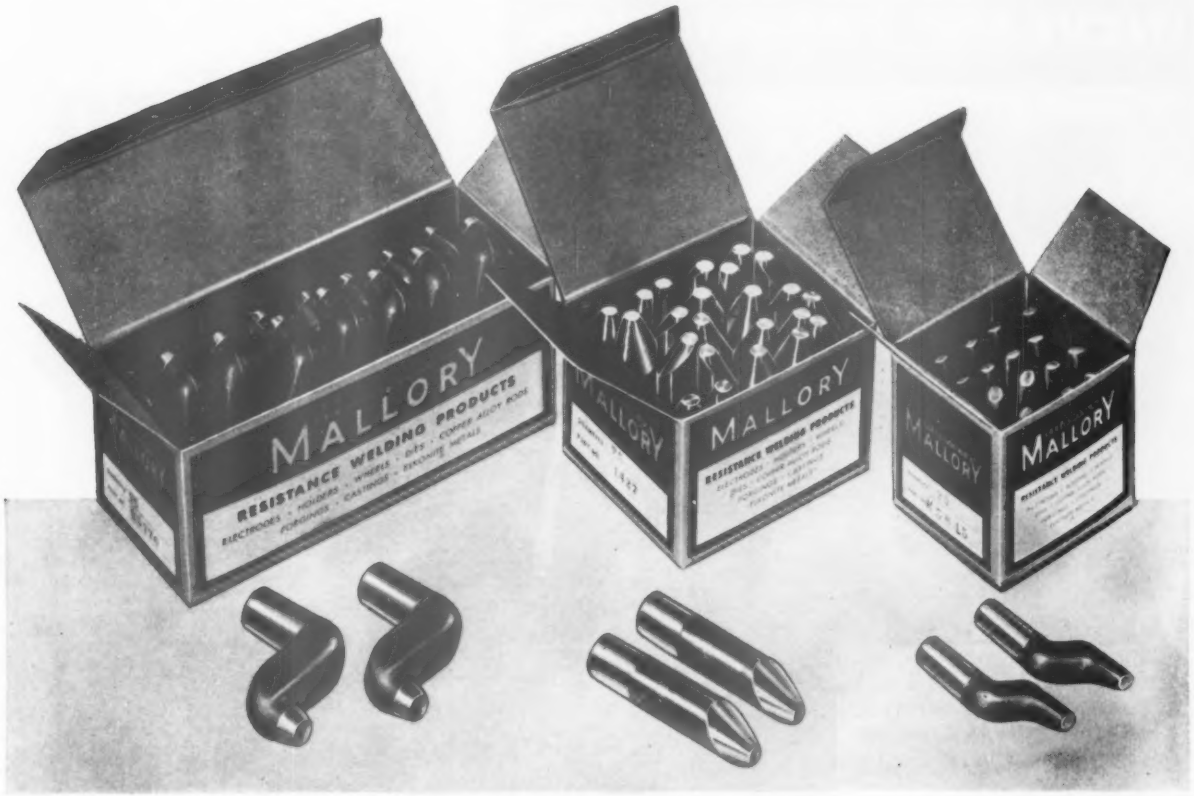

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Volume 33 **AUGUST 1954** *Number 8*

TECHNICAL PAPERS, ITEMS AND REPORTS

Welding Procedure for Welding Tubes in Heat Exchangers, by W. E. Battles 739

New Principles for the Calculation of Welded Joints, by C. G. J. Vreedenburgh 743

Submerged Arc Welding of Chromium-Bearing Steels, by Clarence E. Jackson and Arthur E. Shrubsall 752

Current Welding Literature 758

Spot Welding Thin Aluminum, by I. W. Johnson 759

Resistance Welding in Jet Engine Manufacturing, by F. J. Wallace 762

Correction Notice 767

High Production Applications of Resistance Welding, by C. D. Shultheis 768

The Effect of Welding Variables on Hard-Facing Deposits, by A. Zvanut and V. Peters 778

PRACTICAL WELDER AND DESIGNER

Heliarc and Sigma Welding Team Up 783

Welding Aluminum Sheet 784

Powdered Iron Invades Electrode Designs, by L. K. Stringham 788

SOCIETY AND RELATED EVENTS

Society News 790

Employment Service Bulletin 791

News of the Industry 792

New Literature 801

Personnel 806

New Products 810

List of New Members 818

Abstracts of Current Welding Patents .. 820

Section News and Events 824

Index to Advertisers 828

WELDING RESEARCH SUPPLEMENT

The Welding of Heavy Sections, by W. Spraragen and M. A. Cordovi 369-s

Designing Welded Structural Steel Members Subjected to Cyclic Loading 386-s

Effects of Temperature on Weld Metal Properties, by J. Heuschkel 388-s

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Welding Procedure for Welding Tubes in Heat Exchangers

An experimental assembly setup for the development of suitable procedures and specifications for fabricating tubular heat exchangers

by W. E. Battles

OBJECT

SPECIFICATIONS from customers vary considerably in the procedure and requirements for welding and expanding tubes to tube sheets in the fabrication of tubular heat exchangers. In many instances one customer's requirements are directly contradictory to another's, and although all have apparently logical reasoning, the reasons themselves are often contradictory. There are, of course, different uses for the units and different products used, but this contradiction still exists where the uses are alike.

Some specifications require the tubes to be rolled into the tube sheet before welding, some require the tubes to be welded first and then rolled, others specify rolling before and after welding.

Although ASME Code requirements were not involved in this particular experiment, there are five possible methods of expanded tube connections in the 1952 ASME Code for Unfired Pressure Vessels.

1. Rolled in and beaded.
2. Rolled in, beaded and seal welded.
3. Expanded and flared.
4. Rolled, flared and welded.
5. Rolled and welded without flaring or beading.

The proper procedure became critical with us when we were confronted with the problem of fabricating three tubular heat exchangers, each unit having 2350 two-inch tubes with the specifications that these tubes were to be rolled dry, then welded and then rerolled.

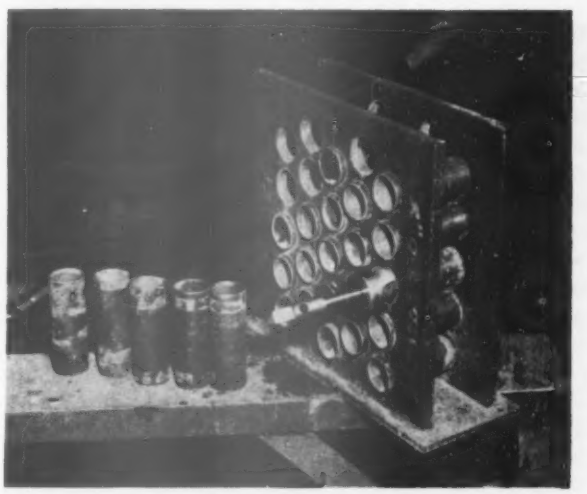

Fig. 1 Experimental unit—before side plates and all tubes were inserted

Considering both ends of the heat exchangers and following the customer's specifications this meant 4700 tube welding operations per unit or over 14,000 tube welding operations for the three units, 28,000 tube rolling operations. Of these, 14,000 were specified by the customer to be rolled dry.

The problem of rolling tubes without any lubrication whatever is self-evident. We, therefore, decided to make up an experimental minature tubular heat exchanger to find the answers to some of the questions involved and to set up a standard procedure for our company which would give us a basis for this procedure and assurance of the required results.

W. E. Battles is Chief Engineer of the Leader Iron Works, Inc., Decatur, Ill.

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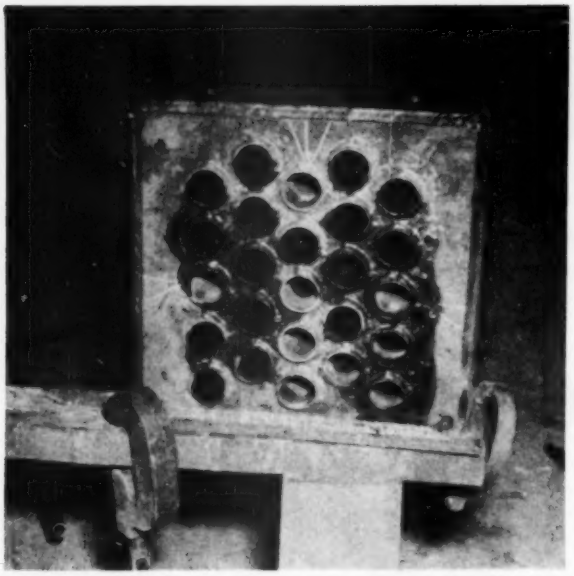

Fig. 2 Experimental unit—tubes in place before welding or rolling

A miniature tubular exchanger assembly was made up duplicating, as nearly as possible, the specifications for the larger units that we were to fabricate.

In considering this report, it should be kept in mind that the particular requirements for which this experimental unit were made, and the conclusions reached, were for operation at relatively low temperatures. Conditions involving stresses of expansion and contraction due to extremely high temperatures could involve some factors not taken into consideration in this experiment. These considerations might involve such factors as preheating or maintaining a relatively high temperature of the material (such as possibly the use of steam inside the unit) during the welding operations. We plan to do some additional experimental work on this phase in the near future.

This small experimental unit was made up of two $^{3}/_{4}$-in thick carbon steel plates 16 in. square as tube sheets. Each plate was drilled and reamed with 25 holes $2^{1}/_{32}$ in. diam for 2-in. OD tubes, spaced on $2^{5}/_{8}$-in. centers on a 60-deg triangular pitch. The holes were not countersunk.

These plates were then welded to a base plate. They were spaced $3^{3}/_{4}$ in. apart, face to face, inside. Two sides and a top were then welded to these plates to permit testing the assembly under pressure. Two $^{3}/_{4}$-in. holes were drilled and tapped in the top cover plate for attachment of water connections and gages.

Figure 1 shows this unit before the side plates were installed and before all the tubes had been inserted.

The tubes used were 2-in. OD x 11 BW gage, 6 in. long of seamless carbon steel. They were placed in the assembly extending $^{3}/_{8}$ in. on each side beyond the outside faces of the tube sheets.

This unit, with all the tubes in place but without any welding or rolling of the tubes is shown in Fig. 2.

This assembly gave us 50 tube-to-tube sheet joints with which to experiment.

Fig. 3 Experimental unit—showing rolling of tubes

The problems which most interested us were as follows: First, can the tubes be welded to the tube sheets satisfactorily before rolling? Second, is it necessary to roll the tubes after welding? Third, if rolled before welding, is it necessary to reroll after welding? Fourth, if the tubes are rolled before welding is it necessary that this be done without any lubrication in order to obtain satisfactory welding?

The reason given us for the specification that the tubes should be rolled without any lubrication before welding was that if a lubricating oil was used in the first operation, the oil would flow around the outside of the tubes and difficulty in obtaining a satisfactory weld would be experienced, due primarily to the sulfur content of the lubricating oil. Therefore, to overcome this problem we decided to try mineral oil as lubricant on some of these joints for rolling before welding.

The 50 tube-to-tube sheet assemblies were divided into six different procedures:

1. Two units—welded only (no rolling before or after welding).
2. Four units—rolled dry, welded, then rerolled.
3. Four units—rolled with mineral oil, then welded (no rerolling).
4. Nine units—rolled with lubricating oil, welded then rerolled.
5. Twelve units—rolled with mineral oil, welded then rerolled.
6. Nineteen units—welded (without rolling first), then rolled.

All tubes rolled in after welding were rolled using lubricating oil as the lubricant.

The test unit was so placed for welding that the welder would, as nearly as possible, duplicate the actual conditions that would be encountered on the large production units.

Photographs were taken of the various operations. The rolling of these tubes is shown in Fig. 3.

Fig. 4 Experimental unit—showing welding of tubes

Before welding the tubes that had been rolled using either lubricating oil or mineral oil as a lubricant, excess oil was wiped off with a cloth but no other cleaning was done.

The welding on this unit was done with an a-c welder operating at 200 amp and 20 v. The welding rod used was an E6012 rod, $^{5}/_{32}$ in. in diameter. The welding of these tubes is shown in Fig. 4.

On the actual large unit we changed to d-c current using an E 6010 rod. This was done so that more men could work on the unit at one time as we did not have sufficient a-c welding machines available at that time. There was no apparent difference in the results.

For rolling the tubes we used a $1^{7}/_{8}$-in. roller using 90 psi air pressure with a $^{3}/_{4}$-in. hose.

OBSERVATIONS DURING THE PROCEDURE

All operations in the making of this experimental unit were performed by regular workers in the shop who would be working on the production unit. Careful supervision was maintained at all times and notes made of all factors that could have any effect on the final results.

The most important of these observations was a careful check on the question of possible difficulty in welding over lubricating oil and over mineral oil.

Welding done on tubes rolled with lubricating oil did develop many pinholes that had to be rewelded.

The mineral oil had no adverse effects whatsoever on the welding.

Rolling after welding was a considerably slower operation than rolling before welding. Also, in the rolling operations after welding, the roller would bind and would not penetrate as far as the rolling operation before welding. This was, of course, due to the resistance of the weld. This is a rather important factor, not only in the resulting joint obtained, but also in the cost of the entire operation.

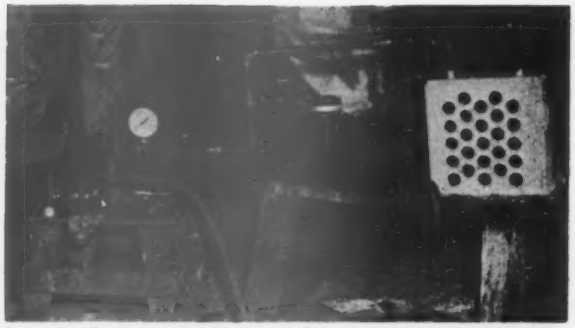

Fig. 5 Experimental unit—showing testing operation

When completed, the test unit was thoroughly cleaned of all traces of oil and two coats of lime wash were applied over the entire unit. This was done with two objectives in mind. First, any small leaks could be more readily detected. Second, any expansion or contraction of the metal, although not noticeable to the eye, would cause the lime wash to crack or flake.

TESTING

The unit was then set up for testing. Water lines and pressure gages were connected to the unit as shown in Fig. 5. The test was conducted with hydrostatic pressure.

The unit was carefully checked at increments of 100 psi pressure. At 150 psi pressure, one very small pinhole developed in the weld of one of the tubes rolled dry, before welding, but not rerolled. At 250 psi pressure, another very small pinhole developed in the weld at one of the tubes that was rolled dry, then welded and rerolled. The pressure was then increased gradually up to 2000 psi but no additional leaks or stresses of any kind were evident, and for these particular production units.

In order to make a more complete study of the actual contact between the tube and tube sheet, a section was cut through this test unit. This section was taken through the center of the tubes in such a way that cross sections were obtained of the following types of joints:

1 section—welded only.
1 section—rolled (mineral oil) then welded (not rerolled).
2 section—welded, then rolled.
1 section—rolled (lubricating oil), welded and rerolled.
1 section—rolled (dry), welded and rerolled.
2 section—rolled (mineral oil), welded and rerolled.

The cross section was cut through with a band saw, polished, and etched with nitric acid. A view of this section is shown in Fig. 6.

RESULTS

From the cross section of this unit a considerable amount of additional information was obtained and the

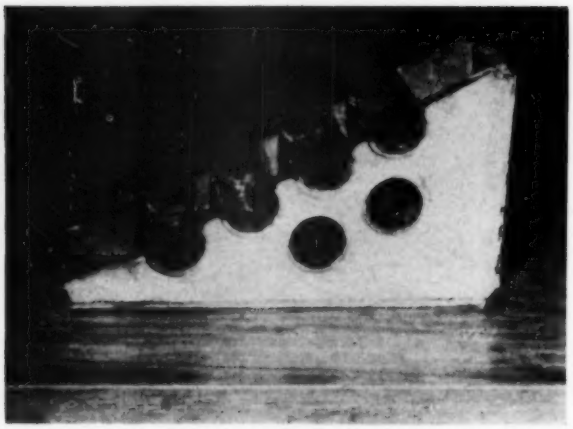

Fig. 6 Experimental unit—showing cross section

observations of this cross section played a major part in determining the specifications to be used for our standard procedure and for these particular production units.

There were definite characteristics of each method and, although very close examination was necessary, these characteristics of each procedure were clearly indicated. In a few cases the characteristics were contrary to previous expectations.

First, and possibly most important, all the joints were tight.

Second, where the tubes were first welded and then rolled, the tube was tighter at the inside edge of the tube sheet than at the outside edge. This was contrary to expectations as it was expected that the weld would keep the roller from penetrating into the tube far enough to exert the required pressure at the inside of the tube sheet. The actual result would indicate that the weld was ductile enough to compress sufficiently to allow the roller to pass into the tube and expand the tube beyond the weld but the strength of the weld was such that the deformation was not permanent. As the tube was not rolled before welding, the joint at the weld was not as tight as it would have been if rolled first.

Third, the section in which the tube was rolled first, then welded, then rerolled showed the joint to be tighter at the outside edge of the tube sheet than at the inside edge. Also, the tubes were slightly flared at the outside edges of the tubes. From this it would appear that rolling first gave a good tight joint and the welding held this joint tight. This joint being tighter than in the case described previously, where the tube was not rolled first, the weld held this joint in its tightly rolled condition and when rerolled there was no expansion of the tube possible. Therefore, the roller could not penetrate much beyond the weld, which caused the tube to flare at the outside edge and to pull away from the tube sheet slightly at the inside edge of the tube sheet.

Fourth, the joint which was rolled first and then welded without rerolling was uniformly tight the full width of the tube sheet. From the results of the other sections previously mentioned, this now appears as the logical condition.

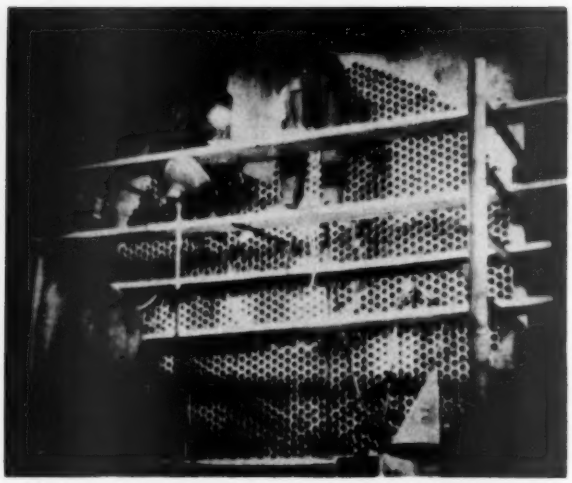

Fig. 7 Production unit—during fabrication

SUMMARY

When the tube was welded before rolling, the joint was not tight when welded and the resistance of the weld would not permit rolling to make the joint any tighter at the welded surface.

When the tube was rolled first, then rerolled after welding, the weld with the joint tight would not permit the roller to pass through on the rerolling operation. This deformed the tube causing the tube to flare at the outside edge and pull away from the tube sheet at the inside of the tube sheet.

A good tight joint is obtained by rolling then welding, but this joint is distorted by rerolling as shown in the third observation of the results obtained.

By rolling the tube to the tube sheet first, obtaining a good tight joint and then welding, the joint was sealed tight and the rolled section remained tight throughout the full thickness of the tube sheet.

From the results of this experiment we established a detailed standard procedure which specifies that tube-to-tube sheet assemblies shall be made up by first rolling (using mineral oil), then welding without rerolling.

This procedure was used on the large units then going into production as shown in Fig. 7. The results were entirely satisfactory and in accordance with those obtained in the experimental unit. These large units have given no difficulty in service.

ACKNOWLEDGMENT

The author wishes to express his appreciation to W. E. Spires, of the Estimating Department of Leader Iron Works, Inc., for the photographs taken during this experiment.

New Principles for the Calculation of Welded Joints

A method for the calculation of the strength of welded joints by an empirically determined limit curve

by C. G. J. Vreedenburgh

Summary

For the calculation of the strength of welded joints, use has been made of a critical surface of the ultimately allowable values for the average stresses p either in the smallest longitudinal section (throat section) or cross section of the weld.

In statically loaded structures the shape of the critical limit curve for loads perpendicular to the longitudinal axis of the weld can be deduced from the experiments of the Netherlands Foundation for Applied Scientific Research (T.N.O.). As the shape of the surface of limits, a surface of revolution (pearoid) has been assumed, of which the meridian section forms the above-said limit curve (see Fig. 4).

Starting from the average stress in the throat section of the weld no theory of rupture or plastic flow has up to now given a satisfactory explanation of all symptoms of rupture observed in end-fillet welds at the moment of fracture. This is caused to a great extent by the uneven distribution of stresses.

In comparison with Kist's method of calculation in use at present in the Netherlands, the principles here presented allow not only some simplification but above all economy in the case of weld sections predominantly loaded in shearing or compression.

In dynamically loaded structures a symmetrical surface of revolution has been adopted as the shape of the critical surface of allowable effective stresses (see Fig. 10). This effective stress is to be obtained by multiplying the numerically greatest value of the oscillating stress by a coefficient γ, which is coherent with the fatigue diagram of the material and the type of weld. The values of γ are given both for butt and fillet welds for two frequency ranges of load oscillations.

1. INTRODUCTION

AT THE second congress of the International Association for Bridge and Structural Engineering, which was held at Berlin in 1936, Prof. N. C. Kist made a valuable contribution on the subject of calculation of welded joints which has attracted widespread attention.[1] Not only did his calculation method bring about a great improvement on the then existing principles but it was applicable in practice while the theory fairly agreed with a great number of test results carried out.[2] The principles for calculation of welded joints in the Netherlands have since then been based on the Kist method. Meanwhile Kist's tests were incomplete to the extent that the behavior in the compression zone of the weld zone—where the normal stress is negative—still required further investigation. In this connection the author of this paper came forward in 1942 to carry on Kist's work.

A start was made with careful study of the work already carried out while new apparatus was being designed for the supplementary tests. Also, the broken test pieces resulting from Kist's work were still available and were subjected to examination.

As a result of this examination we came to the conclusion that the fracture faces of the broken welded joints showed not only various irregularities but did not as a rule coincide with the throat section of the weld triangle.

Thus it appeared desirable to alter the basis of the Kist method in such a way as to avoid the initial assumption that the throat section is always the critical (fracture) plane.

In the meantime a series of preliminary tests, indicated that in pure shear of the throat section, the breaking stress is appreciably greater than 0.58 σ_{brt} where σ_{brt} is the breaking stress in pure tension.

In this connection it was decided also to investigate whether a limiting stress theory other than that of Huber and Hencky might not yield a better explanation of the phenomena observed.

After the liberation of the Netherlands the disordered state of our country was such that three years passed before the plan for an extended series of tests could be carried out—tests which were to investigate the

C. G. J. **Vreedenburgh** is Professor of Engineering Mechanics, Technological University of Delft, Netherlands. He is chairman of the Working Group for Reinforced Concrete and Steel Structures of the Committee for Investigation of Structures of the T.N.O. and chairman of Sub-Committee 36-W-a for revision of Chapter VIII "Design and Calculation of Welded Joints" of the Netherlands Tentative Standard V 1062: *Manual-Arc Welding.*

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strength of end-fillet welds loaded at right angles to the weld, both in the tensile and compressive zones.

Since during this investigation four test pieces were broken in each loading direction, a good picture could be obtained of scatter in the results. Moreover, the disposition of the planes of fracture was determined. The investigation was later extended by two series of tests to determine the reduction of the breaking strength as the weld thickness was increased. The most important conclusion which could be drawn from the extended series of tests carried out by T.N.O. was that the critical limit curve of the breaking stresses for end-fillet welds, particularly in the compression zone, deviated considerably from the ellipse given by the Huber-Hencky theory, while the shear strength amounted to approximately 0.8 σ_{brt} and not 0.58 σ_{brt}. Further, it became evident that, at least for the tensile zone, the limiting stress theory based on the greatest strain gives a better explanation of the observed phenomena than does the theory based on strain energy.

In the meantime there appeared in June 1951 the Standards Tentative V 1062 of Committee 36 (Committee for Standardization in the Field of Welding) which in Chapter VIII dealt with design and calculation of welded joints and gave various directives. In this the attempt was made to adapt the result of T.N.O.'s tests to Kist's calculation method as well as possible.

Due, however, to criticism received, it was decided to revise the above chapter, for which purpose Sub-Committee 36-W-a was set up in January 1952, under the chairmanship of the author.

The most important alteration made is that, as a basis for calculation of welded joints under static loading, no fracture or limiting stress theories are used, but in their place an empirically determined limit curve. Moreover, principles have been considered for the calculation of welds under dynamic load.

The radical suggestion of disregarding not only Kist's method, but also all limiting stress theories, particularly for calculation of fillet welds, is based on the fact that the author has conscientiously analyzed the results of the various rational theories that have been presented for stress analysis. He has compared these results with carefully considered tests and reached the conclusion that they are not compatible. So some sort of an empirical solution must be adopted.

2. TESTS CARRIED OUT BY T.N.O.

These may be considered as an extension of the tests carried out by C. D. Jensen[3] and N. C. Kist. A large number of end-fillet weld test pieces were loaded to fracture, the angle of the transmitted load varying from $\alpha = +90°$ to $\alpha = -87°$. The cross section of the weld was a right-angled isosceles triangle. To vary the direction of the load, appliances were used which were in principle similar to those used in Jensen's and Kist's tests.

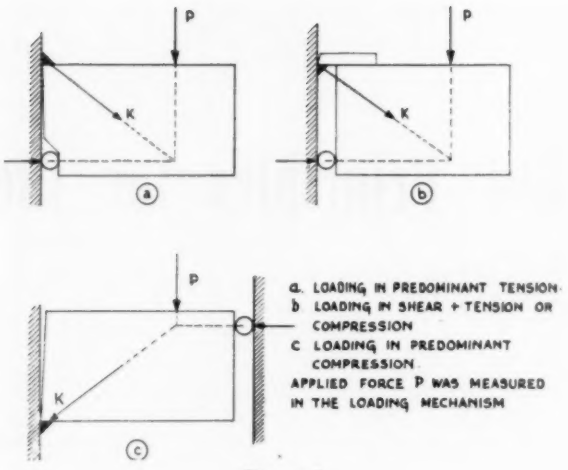


Figure 1

Loading of the weld triangle may be divided into three groups, viz.:

(*a*) Loading with predominant tensile component.
(*b*) Loading with predominant shear component combined with a smaller tensile or compressive component.
(*c*) Loading with predominant compression component.

The loadings (*a*), (*b*) and (*c*) were obtained with the apparatus as shown diagrammatically in Fig. 1 under (*a*), (*b*) and (*c*) respectively. From Figs. 2 and 3 may be seen how the apparatus is mounted in the loading rig.

Transmission both of the applied force and the horizontal reaction was obtained by the use of hardened steel rollers (diam 5 cm) and hardened steel roller ways, with which there was no bearing friction. The rolling resistance factor was carefully measured and found to be about 0.01. This gives a maximum alteration of about 0.5° in the line of action of the load and an alteration in the force of about 1%, so that friction effect can be legitimately neglected.

All welds were executed with a quick-flowing electrode of the "ferrimantle" type (E 6020-electrode, iron oxide type), with a diameter of 6 mm as single-pass welds.

Three series of tests were made. The first and most extensive series had as its object the systematic determination, with the help of the above-described equipment, of the critical polar diagram of the breaking stresses.

The throat of the isosceles end-fillet weld was in this case 4 mm (a throat of 4 mm agrees with a leg length of $^{7}/_{32}$ in.).

In the second series, the breaking stress of cross fillet joints and double-lap joints was investigated, while the third series was to determine the effect of weld-thickness on the breaking strength. The throats here were 3, 6 and 9 mm, respectively.

In the first series, the strains as well as the forces P were measured. This was done with the three dial

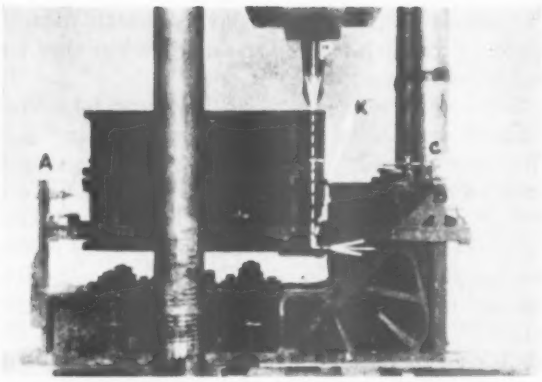


Fig. 2 Loading of a test specimen (tension)

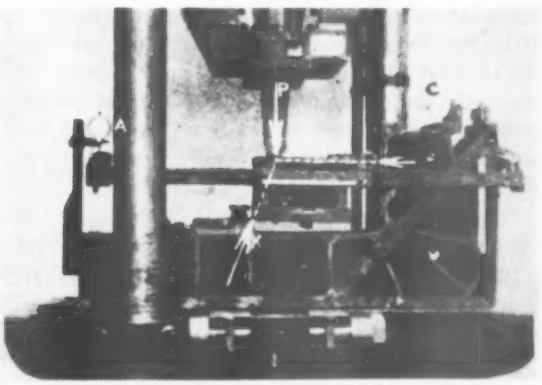
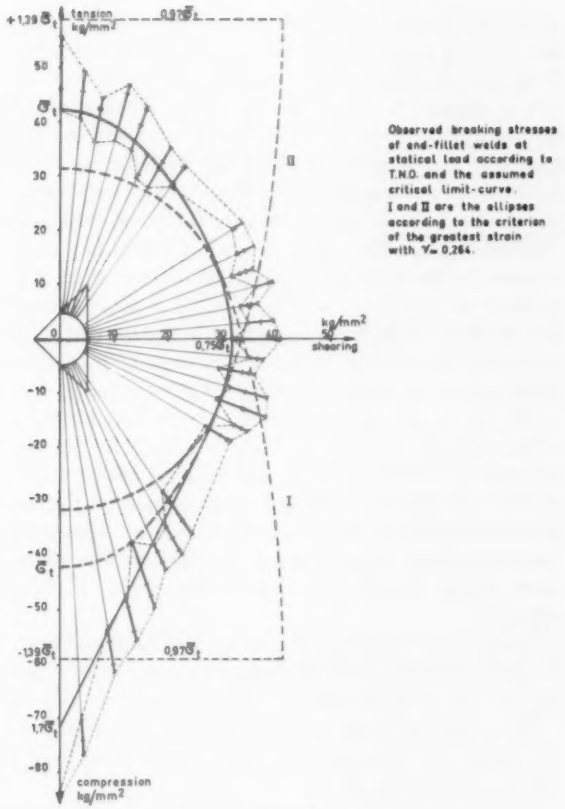


Fig. 3 Loading of a test specimen (compression)



Observed breaking stresses of end-fillet welds at statical load according to T.N.O. and the assumed critical limit-curve.
I and II are the ellipses according to the criterion of the greatest strain with $\nu = 0{,}284$.

Figure 4

gages, *A*, *B* and *C*, shown in Figs. 2 and 3. The gages *A* and *B* indicated the deflection of a steel rule which was rigidly attached to the steel block carrying the load *P*.*

Figure 4 shows the polar diagram of the measured breaking stresses referred to the throat section; in other words, the breaking force was divided by the area of the throat section and then set out from *O* in magnitude and direction. Normally four test pieces were broken in each direction.

The first thing to be seen from Fig. 4 is the fairly large scatter of the breaking stresses. The differences between the extreme observations are about 20–25% of the average values.

It emerged further from the tests that the plane of fracture does not as a rule coincide with the throat section, but makes an angle with it. This angle varies with the angle α between the fracturing load and the throat section.

The second series of tests was made, as already remarked, with cross-fillet joints and double-lap joints. These tests showed that if the direction of the transmitted load was statically indeterminate, the fracturing stress was greater than that calculated in the usual way. In addition, it was clearly observed that the strength of the weld decreases as the weld thickness increases.

* Dial gage *C* served to check the deflection of the frame.

On this account, a third series of tests was carried out to investigate systematically the effect of weld thickness on the breaking strength.

To this end, the fracture strength in three directions in the tensile sector was determined for welds of 3, 6 and 9 mm throat, respectively. Five test pieces were broken in each direction. For the small weld throat of 3 mm particularly, the scatter of the observations was very large. The difference between the extreme values was here 50–70% of the average value.

Finally, from these tests a reduction factor was calculated for the breaking stress as the weld thickness increases. This is about 3% for each millimeter by which the throat exceeds 4 mm.

3. INEFFICIENCY OF LIMITING STRESS THEORIES

As already remarked no one limiting stress theory seems to serve as a starting point for our welding calculations and we ask ourselves involuntarily what the reason is for this.

As is known, we are most familiar with limiting stress theories based either on occurrence of fracture or of yielding. Further, in every limiting stress theory the state of stress at a fixed point is considered. Thus, when the appropriate limiting stress criterion is reached at a certain point, fracture (or yield as the case may be)

will occur at that point, but not at other points where the limiting stress criterion has not yet been reached. From this it follows that a limiting stress criterion cannot, in general, be based on an average stress in a cross section unless the state of stress at all points in the cross section is the same.* If the stress distribution in the cross section is not uniform, then it may well happen that at one or more points the limiting stress criterion is reached or exceeded, while this is not yet by any means the case for the average stress. It is known that up to the moment of fracture, the stress in the throat section of the end-fillet weld is far from evenly distributed. The reduction in strength caused by increase of weld thickness must be attributed to this to a great extent.

On the above grounds, we conclude that no theory of limiting stress which holds good initially for the state of stress at a certain point, can be used with reference to the average stress in any cross section of the end-fillet weld, even when the plane of fracture replaces the throat section. The cause of this lies in the nonuniform stress distribution, particularly due to notch effect.

Quite apart from this, it may further be noted that it is not reasonable in the first place to attempt to explain the occurrence of fracture by a theory based on yielding.

Thus, owing to the negative result of all attempts in this direction, we must, as a start, break loose from all theories of limiting stress.

It seems to us that for practical calculation of welded joints a fracture theory may well be dispensed with, provided that an accurate limit curve, determined experimentally, is available.† It is clear that with such a curve there is no objection to working with average stresses.

In the USA no physical criterion is applied; even for butt welds, the ratio of allowable stress for shear to that for tension is taken as 0.725. After examining the development of the mathematical theory of plasticity in recent years, it may be said that the tendency is more and more away from the simplest formulation of the phenomenon of yielding based on distortion energy. The processes acting in the elastic/plastic zone appear to be so complicated that we have to be content with formulations lacking any physical basis.

Most theories confine themselves to a proportional increase of all principal stresses (so-called radial loading) and a simple strain history of the loaded material, while time effects are neglected.[4, 5]

From the above it may be concluded that the time is still far distant when there will be complete clarity of the relationship of various effects in yielding, even for the simplest states of stress.

Until that time, must we adhere to conceptions which clash with observed effects?

Or should we utilize as quickly as possible data obtained by research work, particularly when they can lead to not inconsiderable economies?

We accept therefore as critical limit curve for our calculations the curve shown with a heavy line in Fig. 4. This curve is made up of three parts joining tangentially, namely an ellipse with, as its apex, the point $\sigma = + \vartheta_t$, $\tau = 0$, an arc of a circle with mid-point at 0 and radius $0.75\ \vartheta_t$, and a tangent line to this circle passing through the point $\sigma = -1.7\ \vartheta_t$, $\tau = 0$. The long semiaxis of the ellipse is $2.39\ \vartheta_t$ and the short semiaxis $0.97\ \vartheta_t$. The mid-point of the ellipse is at the point $\sigma = -1.39\ \vartheta_t$, $\tau = 0$ (ϑ_t is the allowable stress in pure tension). As may be seen the form of this figure is largely determined by the smallest values of breaking stress found by us. The form of the ellipse is chosen such that it can be expressed mathematically with the help of the criterion of greatest strain. It is noteworthy that this critical limit curve agrees fairly well with the minimum fracture stresses found in Jensen's tests, at least in the zone where the tensile component predominates.

4. PRINCIPLES FOR CALCULATION OF WELDED JOINTS UNDER STATIC LOAD

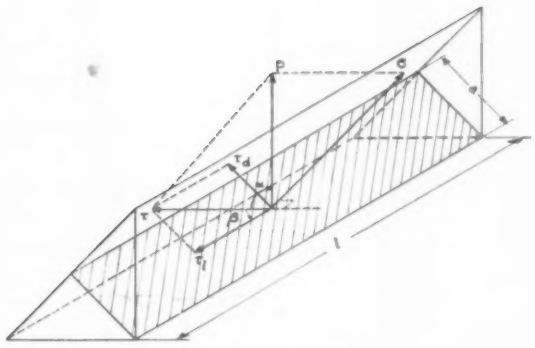
The pear-shaped limit curve given in Fig. 4 refers to the actual breaking stress, in which at the same time the effect of scatter of stress is taken into account, since for the most part of the minimum values are taken as critical. It is clear that, for the allowable stress ϑ_t, reference should be made to those given in our Building Code which incorporate a safety factor of about three, compared to the stress at fracture. We would again point out that, although the strength calculation is based on the average (total stress) p in the throat section, the stress distribution is in reality far from uniform. We know that, owing to the notch effect in the right angle of the weld triangle, yielding takes place at small loads, but is not at this point dangerous. With increase of load the yield zone extends and the maximum strains increase until finally fracture occurs. This whole process is, however, involved in the fracturing stress employed, as is the effect of shrinkage stresses which may be present.

Working with the average stress as yardstick has the further advantage that calculation of the actual stress distribution is not necessary. Although possible in principle (for example by means of Airy's stress function), such a theoretical calculation would have little significance, due to indeterminate factors such as notch form, shrinkage stresses and irregularities in the shape of the weld triangle, to which the large scatter in the test figures must undoubtedly be attributed.

The polar diagram in question is, however, still incomplete in that it gives only the critical stress for a load at right angles to the length of the weld. If the load has a component in the direction of the weld the average stress p will no longer lie in a plane at right

* The further possibility may be considered that a constant relationship might exist between the limiting stress criterion based on average stress and that based on the most dangerous point, for all directions of loading of the weld. This is not the case, however.

† If we do not confine ourselves to loads perpendicular to the longitudinal axis of the weld, a critical limit surface must be available. We shall presently return to this point.



LOADING OF THE SHORTEST LENGTHWISE CROSS-SECTION OF A WELD BY AN AVERAGE STRESS P IN ANY DIRECTION

$$p=\sqrt{\sigma^2+\tau_d^2+\tau_l^2} \qquad \tan\alpha=\frac{\sigma}{\sqrt{\tau_d^2+\tau_l^2}}$$

Figure 5

angles to the weld, but will have, in addition to a normal stress component σ and a cross shear stress τ_d, another shear conponent τ_l (Fig. 5).

The question now is whether our polar diagram also holds good for this arbitrary direction of loading. In this connection the following remarks can be made. According to N. C. Kist's tests the shear stress of transversely loaded end-fillet welds is as large as that for side-fillet welds. For the side-fillet weld in compression Kist even found a much greater shear strength of 0.74 σ_{brt} against 0.62 σ_{brt} for the end-fillet weld.

Vandeperre found for the strength of the side-fillet weld in tension, $^3/_4$ of the strength of the double-lap weld,[6] as did Kist, so that in this respect no discrepancy exists between the two investigators.

Roš, too, determined the strength of the side-fillet weld in tension (Reference 7, page 25). Compared to the double-lap weld and the cross end-fillet weld, for which Roš found equal strengths, he finds the ratio 0.82. If, on the basis of T.N.O.'s second series of tests, the strength of the double-lap joint in tension is taken as $>1.\ldots\ \sigma_{brt}$ and that of the cross end-fillet weld in tension as $>0.93\ \sigma_{brt}$, i.e., on the average thus $>0.99\ \sigma_{brt}$, then the ratio of 0.82 gives for the strength of the side-fillet weld, referred to the throat section, the value $>0.8\ \sigma_{brt}$.

Based on the above, we can take the shear strength of side-fillet welds as equal to that of end-fillet welds. On this account, a surface of revolution will be taken, with the critical limit curve for transverse loading as meridian cross section. For convenience this surface will be known as a *pearoid*.

For the calculation of the stress p which has to be transmitted by the throat section of the end-fillet weld, it is best to reduce the given loading to one or more basic cases. The outside end of the resulting stress vector must then, after correct orientation, remain within the pearoid.

The pearoid may be considered as placed in the weld in such a way that the throat section coincides with the plane $\sigma = 0$ of the surface (see Fig. 6).

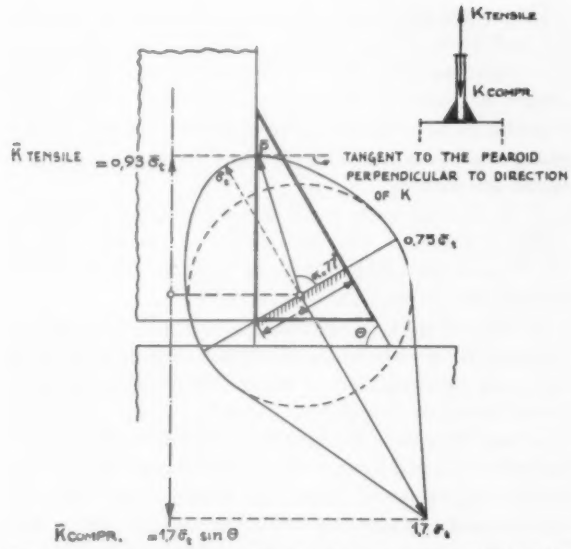


Fig. 6 The Pearoid placed in the throat section of the weld

In this manner the direction of the stress may be determined for statically indeterminate cases if it is assumed, as in N. C. Kist's calculation method, that the average resulting stress adjusts itself in the throat section such that the force to be transmitted becomes as large as possible. In the case of Fig. 6, this direction for tension is given by $\alpha = 77°$, while for compression it is plainly at right angles to the throat section. Here, for the allowable force in the vertical strip is found

$$K_{tens.} = 2 \cdot l \cdot a \cdot 0.93\ \bar{\sigma}_t$$
$$K_{compr.} = 2 \cdot l \cdot a \cdot 1.7\ \bar{\sigma}_t \sin\theta$$

if l and a are the length and throat of the weld, respectively.

Since, as was already remarked, the fracture strength of the end-fillet weld decreases with increase of weld thickness, a reduction to take account of this effect must be made.

In fact, our critical limit curve holds for a weld throat of 4 mm. Since the vast majority of weld throats is not thicker than 5 mm, this reduction was only laid down for weld throats of more than 5 mm. If it is assumed that the allowable limiting stress is reduced 4% for each millimeter that the weld is thicker than 5 mm, this will be on the safe side, not only according to our own tests but also to those of the EMPA[7] and of Vandeperre.[6]

From the last-mentioned tests it is further clear that the reduction in strength with increase of weld thickness becomes smaller and finally approaches a limit. For welds 10 mm and more in throat thickness, therefore, the reduction should not be more than 20%. For the rest, it is clear that, already owing to this decrease in fracture strength, employment of thick end-fillet welds should be avoided.

It is self-evident that the given allowable stresses only hold good if, during welding, the necessary measures are taken against dampness, draught, temperature, etc.

It is extremely desirable to avoid as far as possible the bad effect of the beginning of the weld and the crater at its termination, either by continuing these in a weld-groove in pieces of material tack welded to the piece actually to be welded or, for example, where a strip is being welded on edge to a plate, by continuing the weld round the corners.

It is further quite possible to avoid, by a suitable welding method, the formation of craters without these methods. The minimum weld length should be 8 times the throat thickness of the weld.

Finally, it may be assumed on a basis of theoretical calculation that the optimum length for side-fillet welds from the point of view of stress distribution should be about twice the breadth of the strip.

It may be asked whether the pearoid, which is based on tests with end-fillet welds, may be used also for butt welds. It appeared from Kist's tests that both the tensile and compressive strengths of butt welds may be taken as large as those for end-fillet welds. To use for calculation of butt welds, for example, the yield criterion, and for calculation of end-fillet welds the fracture criterion which is interpreted geometrically by the pearoid, would be to fall between two stools. On this account it seems to us desirable to use the pearoid for the butt welds as well as for fillet welds. Since, however, for butt welds which after completion form a joint without a crevice between the two parts to be united, a much more uniform stress distribution exists in the throat section than in fillet welds, due to the almost complete absence of notch-effect, no allowance need be made for reduction in strength as the weld thickness increases.

It must further be pointed out that the pearoid gives the allowable stress in the throat section of the weld when no normal stress exists in the axial direction of the weld.

In built-up sections, however, the side-fillet weld occurs frequently, which, besides a normal stress acting along the length of the weld, also has a shear stress τ_l to transmit in the throat section in the longitudinal direction of the weld. This shear stress manifestly acts together with an equal shear τ_l acting in the cross section of the weld (Fig. 7). Since the pearoid gives the allowable stress vector made up of a normal and shear stress, the condition here is that $p_a = \sqrt{\sigma_l^2 + \tau_l^2}$ acting in a direction given by $\tan \alpha = \sigma_l/\tau_l$, must remain inside the pearoid. In this case the σ-axis of the pearoid must be placed perpendicular to the transverse cross section of the side-fillet weld.

It may be pointed out that the allowable value of $-1.7\ \delta_l$ for pure compression is connected with the three-dimensional state of stress developed in the end-fillet weld in compression. It is self-evident that in cases where the development of this state of stress cannot be assumed, e.g., in the case of a side-fillet weld of a built-up I-section loaded lengthwise in compression, δ_d must be put $= -\delta_l$.

In the compression zone, the linear limit of the curve is then replaced by the ellipse II of Fig. 4.

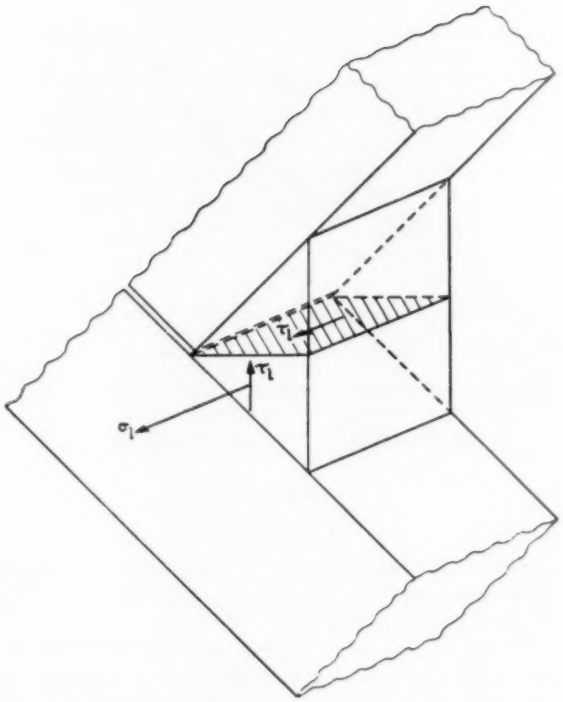


Fig. 7 Side-fillet weld with normal and shear stress in the transverse cross section

5. DYNAMIC LOADING

Much has already been published on the fatigue strength of welded joints. Test results, however, diverge considerably and are not very comprehensive. No agreement exists yet as to the influence of shrinkage stresses on the fatigue strength of welds. Due to this, it would be a defensible attitude that the time is not yet ripe to lay down general principles for the calculation of welded joints for dynamic loading. The fact remains, however, that welded joints are much used for cyclical loads and it seems better to give the designer simple principles to which he can adhere, rather than to leave him in complete uncertainty.

To make the following clear, and for the convenience of the reader, a few points in connection with the Goodman fatigue diagram and its use will first be recalled.

A material is subjected to a certain number of stress cycles n (e.g., 10^6) between the limits s_{max} and s_{min}. The average value of these stress limits, $(s_{max} + s_{min})/2$, is called the average stress or static prestress s_g. The half-amplitude of the stress fluctuations is called the semirange of stress s_r. Thus:

$$\left.\begin{aligned} s_r &= \frac{s_{max} - s_{min}}{2} \\ s_g &= s_{max} - s_r = s_{min} + s_r \end{aligned}\right\} \quad (1)$$

The amplitude of the stress fluctuations (or the appropriate value of s_{max}) with which fracture occurs in every case after the same number of stress cycles n, may now be determined for different values of s_{min}. It will then be found that this amplitude becomes

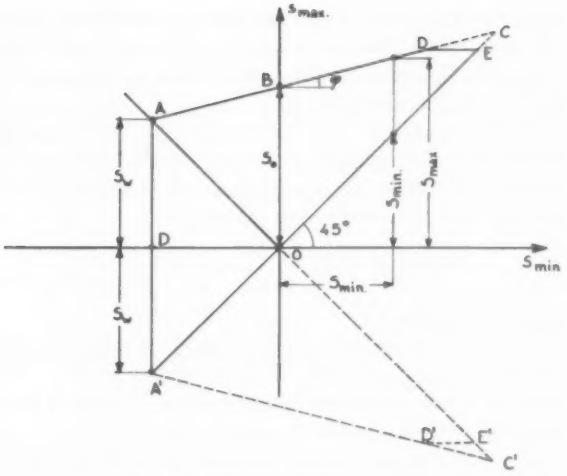


Fig. 8 Goodman fatigue diagram

smaller as s_{min} increases. Each corresponding pair of limit values may be plotted in its own vertical line on a rectangular coordinate graph with s_{min} as abscissa. Such a diagram as shown in Fig. 8 is obtained in this way. This diagram is attributed to Goodman. The allowable stress fluctuation between a positive limiting value and a negative value of the same magnitude is known as the reversed strength s_w, while the allowable stress fluctuation between 0 and the limit value above it is known as the jump strength s_0. If the numerically greatest values of the stress limits are compressive stresses, the Goodman diagram can be completed with the part $OA'C'$ as shown in Fig. 8 with dotted lines. The fatigue diagram is wholly dependent on the number of cycles n which has been chosen as basis. It may be said that for a fixed value of s_{min}, the corresponding value of s_{max} will, in general, increase as n decreases and vice-versa. Usually the point of the diagram near C or C', as the case may be, is cut off by a horizontal line as shown in Fig. 8 (lines DE and $D'E'$).

In practice it is convenient to multiply the numerically greatest stress s_{max} corresponding to a certain value of s_{min} by a coefficient γ and to impose the condition that γs_{max}, the so-called effective stress, does not exceed a certain fixed allowable value which is independent of s_{min}.

For a certain number of cycles n, γ is clearly dependent on the ratio s_{min}/s_{max}.

We may write in general

$$\gamma = a - b \cdot \frac{s_{min}}{s_{max}} \qquad (2)$$

in which a and b are constants still to be determined, and $\gamma \geqq 1$. Further

$$\gamma \cdot s_{max} = \bar{s} \qquad (3)$$

if $\bar{s}$ is the allowable stress referred to above. From (2) and (3) follows

$$\bar{s} = a \cdot s_{max} - b \cdot s_{min}$$

$$s_{max} = \frac{s}{a} + \frac{b}{a} \cdot \bar{s}_{min}$$

so that with reference to the Goodman diagram (Fig. 8) we may write

$$\bar{s} + \frac{b}{a} s_{min} = s_0 + s_{min} \tan \varphi \qquad (4)$$

The constants in (2) are thus fixed by the relationships

$$\left.\begin{aligned} a &= \frac{\bar{s}}{s_0} \\ b &= a \tan \varphi = \frac{\bar{s}}{s_0} \tan \varphi \end{aligned}\right\} \qquad (5)$$

Given a Goodman diagram as in Fig. 8, the values of a and b can be calculated from (5) with a chosen value of the allowable stress $\bar{s}$, and if desired the linear variation of γ with s_{min}/s_{max} may be shown graphically.

In dynamic loading differentiation must be made between butt welds and fillet welds. For the former category of welded joints the fracturing fatigue stresses can be taken significantly higher than for the latter. If, for example, the tensile jump strength for 10^6 cycles is 1700 kg/cm² for butt welds, then the value for fillet welds certainly cannot be assumed higher than 1100 kg/cm². In this connection it may be remarked, however, that a side-fillet weld loaded by normal stresses in the longitudinal direction such as occurs, for example, in built-up sections in bending, may be treated as a butt weld as far as strength calculation is concerned.

Assuming a safety factor against fatigue of 1.5 and taking tan φ in the Goodman diagram as about 0.55*, the following result for 10^6 cycles (with $\bar{s}_t$ = 1400 kg/cm² for pure tension) is arrived at for the butt weld

$$\gamma_1 = 1.25 - 0.70 \frac{p_{min}}{p_{max}} \qquad (6)$$

and for the fillet weld

$$\gamma_2 = 1.90 - 1.05 \frac{p_{min}}{p_{max}} \qquad (7)$$

In both cases, for pure tension (with a minimum value of $\gamma = 1$) the following condition must be met:

$$\gamma \cdot p_{max} \leqq \bar{\sigma}_t \qquad (8)$$

if the weld is to be sufficiently strong.

If $\bar{\sigma}_t$ = 1400 kg/cm² and with $p_{min}/p_{max} = 0$, the allowable tensile jump stress in the butt weld is found to be (1400/1.25) = 1120 kg/cm², giving a safety factor of 1.52 compared to the ultimate tensile jump strength of 1700 kg/cm². In the same way for the fillet weld the allowable tensile jump stress is found to be (1400/1.90) = 735 kg/cm², corresponding to a safety factor of 1.5 with regard to the ultimate tensile jump strength of 1100 kg/cm². Further from formula (6) tan φ = (0.70/1.25) = 0.56 and from formula (7) tan φ = (1.05/1.90) = 0.55.

It will be readily seen that the minimum value of γ_1 or γ_2 = 1 cuts off the allowable varying stresses in the Goodman diagram for pure tension at a height of $s_{max} = \bar{\sigma}_t$.

If the average stress p to be transmitted by the

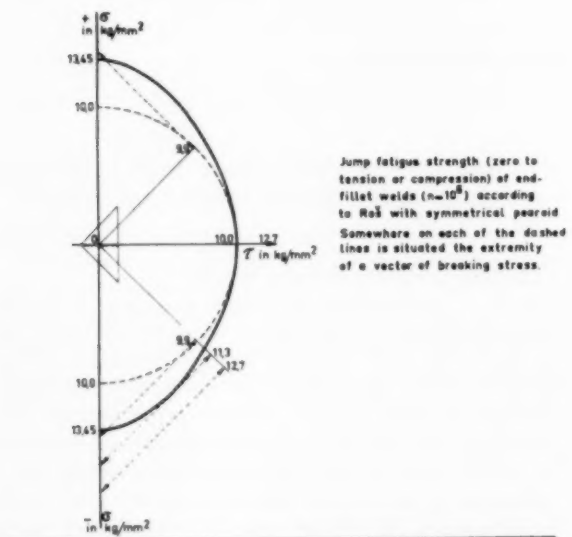
* This value may be taken as an average of results obtained by various investigators.

throat section acts in any direction, a limit surface for the fracturing stresses is required in the same way as for static loading, inside which surface the critical stress vector $\gamma \cdot p_{max}$, must lie. The question is, however, whether our pearoid for static loading will also serve as this surface. In the first place it must be pointed out again that although the yield criterion appears usable for the calculation of fatigue strength of steel, this can no longer be the case if the average stress in the throat section is used as a yardstick when the stress distribution is not uniform.

As far as we know, no tests have yet been carried out to determine the form of the critical limit curve of the breaking stresses for dynamic loading in the same way as was done by T.N.O. for static loading of fillet welds.

The only tests known to us from which conclusions can be drawn are those of Roš.[7, 8] The repeated strengths in kg/cm² found for the throat section of the single weld have been collected in Fig. 9 where they are also graphically represented. They are for 10^6 load cycles.

Taking into account the fact that the directions of the forces to be transmitted by the throat section of the welds are not necessarily parallel to the external tensile or compressive force acting on the joint, the assumption of a circle as polar limit curve of fracturing stresses and a sphere as limiting surface has a great deal to be said in favor of it, at least for fillet welds. In this connection, for dynamic loading of butt welds in which the limit surface will have more the form of an ellipsoid



Joint	Description	Tension kg/cm²	Compression kg/cm²
compression — tension	a cross end-fillet weld	990	1270 1130
compression — tension	b joint with end-fillet welded straps	990	990
compression — tension	c joint with side-fillet welded straps	1270	1270

Figure 9

with its long axis in the σ-direction, a symmetrical pearoid lying inside the sphere may be taken as a safe limit surface both for butt welds and for fillet welds, as given in Fig. 10 (compare Fig. 4).

To determine the allowability of a cyclic stress in any direction on the throat section of a weld—varying between the limits p_{min} and p_{max}—the critical stress $\gamma \cdot p_{max}$, acting in a direction given by

$$\tan \alpha = \frac{\sigma_{max}}{\tau_{max}} = \frac{\sigma_{max}}{\sqrt{{\tau_d}^2_{max} + {\tau_l}^2_{max}}}$$

is first determined.

For 10^6 load cycles γ_1 for butt welds is taken according to (6) and γ_2 for fillet welds according to (7). The critical stress vector arrived at in this way must then lie within the symmetrical pearoid (Fig. 10).

From the larger γ-value for fillet welds it is evident that these, and in particular end-fillet welds are less suitable for taking up load fluctuations, so that they should be avoided for dynamic loading. This is not, however, always possible.

For a side-fillet weld loaded as shown in Fig. 7, the pearoid must clearly again be placed with the σ-axis perpendicular to the transverse cross section of the weld

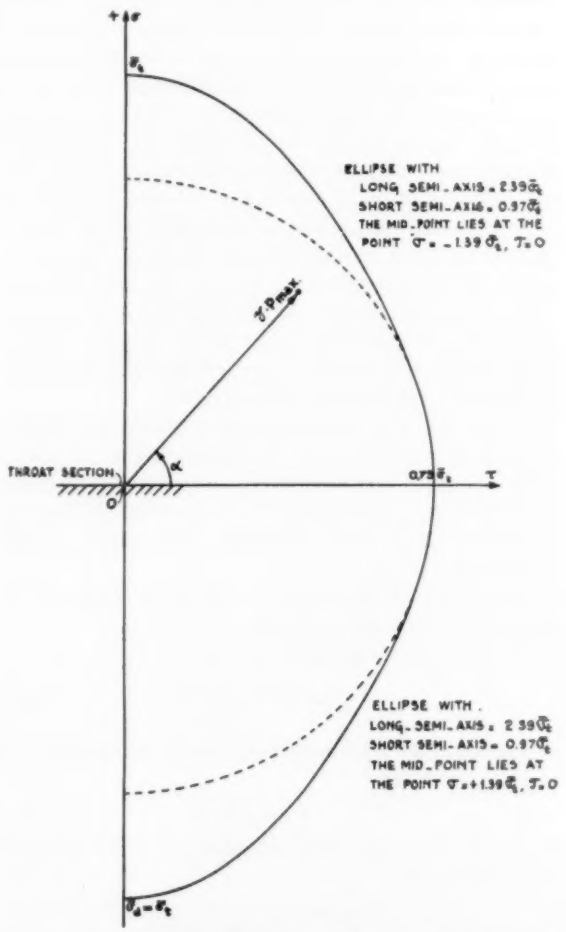


Fig. 10 Symmetrical Pearoid for dynamic loading. The limiting stress vector γp_{max} must lie inside the limit curve

in such a way that this section coincides with the plane $\sigma = 0$.

It is obvious that in this case the value γ_1 should be used in conjunction with $\sigma_{l\ max}$ for butt welds, and the value γ_2 in conjunction with $\tau_{l\ max}$ for fillet welds. A shear stress of the same magnitude is then also present in the smallest longitudinal cross section of the weld.

Owing to this, the condition for allowability is that the stress vector

$$\gamma\ p_{max} = \sqrt{(\gamma_1 \sigma_{l\,max})^2 + (\gamma_2 \tau_{l\,max})^2}$$

the direction of which is given by

$$\tan \alpha = \frac{\gamma_1 \sigma_{l\ max}}{\gamma_2 \tau_{l\ max}}$$

must lie within the pearoid.

Using (6) and (7) and for 10^6 cycles, we find for the allowable jump normal stress $\sigma_{l\ max} = (1400/1.25) = 1120$ kg/cm^2 and for the allowable repeated shear stress

$$\tau_{l\ max} = \frac{1400 \times 0.75}{1.90} = 552 \text{ kg/cm}^2$$

The ratio of these is $552/1120 = 0.495$, which agrees very closely with the ratio 0.50 found experimentally by Roš.[8]

Since dynamically loaded structures occur which only have to take up less than 10^6 cycles of load in their period of depreciation it is desirable to lay down for both butt and fillet welds, further γ-values which, for example, hold for a number of cycles between 10^5 and 10^6. In this case we can put

$$\gamma_1 = 1.15 - 0.65 \frac{p_{min}}{p_{max}} \qquad (9)$$

$$\gamma_2 = 1.45 - 0.80 \frac{p_{min}}{p_{max}} \qquad (10)$$

again, of course, with a minimum value of 1 for both γ's.

If, during its period of depreciation the number of load cycles is less than 10^5, the structure can be considered as statically loaded, except in certain cases. It should be remembered that, in the safety factor of 3 from the point of view of fracture in the calculation of statically loaded structures, a considerable, normally occurring, cyclic load is tacitly provided for.

As concerns the reduction in strength of fillet welds with increase in weld thickness, the same rules as for static loading should be used.

6. CONCLUSION

The principles given here formed the basis of a suggested revision of Item VIII of V1062 in the Netherlands. Since attention has been paid to dynamic loads, the new principles are more complete than the existing ones. Compared to the existing requirements they offer a not inconsiderable economy of material. This saving is for $\alpha = 0°$, 40%; $\alpha = 30°$, 31%; $\alpha = 60°$, 11%; $\alpha = 90°$, 0%. If for negative σ the asymmetrical pearoid can be used, the savings, particularly for $\alpha < -60°$, are still larger. It is clear that to estimate the actual saving of material which is attainable in practice with this new calculation method, the fact must be taken into account that welded joints loaded predominantly in tension or compression occur more frequently than those loaded in predominant shear.

We cannot but acknowledge that with these new principles for calculation of welded joints the last word has by no means been said. Systematic tests should be carried out to determine the exact form of the critical limit surface for static loading, particularly to determine whether this is, in fact, a surface of revolution under all circumstances.

Just as the fracturing stress of an end-fillet weld is influenced by its thickness, it may be expected that the strength of a side-fillet weld is also more or less dependent on its length.

For dynamic loading, it must be further investigated to what extent the symmetrical pearoid can be retained both for butt and for fillet welds.

Strictly speaking, no damage to the material will occur if the distribution graph of the varying loads for the life of the structure lies beneath the so-called damaging line.[9, 10] This line has a similar course to the Wöhler curve but lies beneath it and runs into it at about 10^7 fluctuations. Although this damage theory is very attractive, it seems to us that it is not yet possible to incorporate it in principles for practical application.

Whether the calculation principles given by us yield sufficient guarantee of safety depends, even more for dynamic loading than for static loading, on the design and manner of execution of the welding. No calculation principle or safety factor can counterbalance a poorly welded joint, whether due to a fault in design or execution.

ACKNOWLEDGMENT

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Submerged Arc Welding of Chromium-Bearing Steels

The influence of current, voltage and speed of travel on weld area in chromium bearing steels and recovery of chromium, silicon, nickel, manganese and other alloys

by Clarence E. Jackson and Arthur E. Shrubsall

Abstract

In the submerged arc welding of chromium-bearing steels, many of the relationships of welding technique to weld performance which have been suggested for mild steels generally apply. The chromium content of a six-pass weld deposit is 80% that of the electrode when using Unionmelt Grade 80 welding composition and it is 80% that of the electrode plus 1.25% when using Unionmelt Grade 88 welding composition. At a given chromium level, lower silicon content of weld metal is obtained when using Grade 88 than with Grade 80 welding composition. The use of these relationships should aid in improving the control of weld metal analyses in the submerged arc welding of chromium-bearing steels.

INTRODUCTION

THEORETICALLY, in a completely neutral atmosphere, a steel could be welded with no loss of elements. In practice, our closest approach to a neutral welding atmosphere is attained by use of inert gas protection. In shielded-metal-arc welding, coatings are formulated to adjust for losses or to provide the desired additions. In submerged arc welding, some welding compositions are designed to give relatively neutral protection. Others are intentionally formulated to give control over such alloys as manganese, silicon and carbon. The use of fully automatic control of welding techniques permits uniform maintenance of conditions.

Clarence E. Jackson and **Arthur E. Shrubsall** are Research Engineers, Metals Research Labs., Linde Air Products Co., Niagara Falls, N. Y.

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OBJECT

The influence of such factors as welding current, voltage and speed of travel on penetration and area of weld metal in welding mild steel has been reported in literature;[3] however, little effort appears showing these general relationships for chromium-bearing steels. The data from the literature, particularly those of Kerr and Elmer,[4] together with additional data from these Laboratories, serve as a basis for the relationships described in the first portion of the paper. The second portion deals with recovery of chromium, as well as silicon, nickel, manganese and other alloys associated with chromium in steels carrying up to 25% chromium.

GENERAL TESTING PROCEDURES

All welding operations reported in this study were performed using commercial welding equipment with automatic control of welding current, welding voltage and speed of travel. Welds were made using many variations in welding technique, including surface beads, full welds and built-up pads such as shown in Fig. 1. Unionmelt Grades 80 and 88 welding compositions were

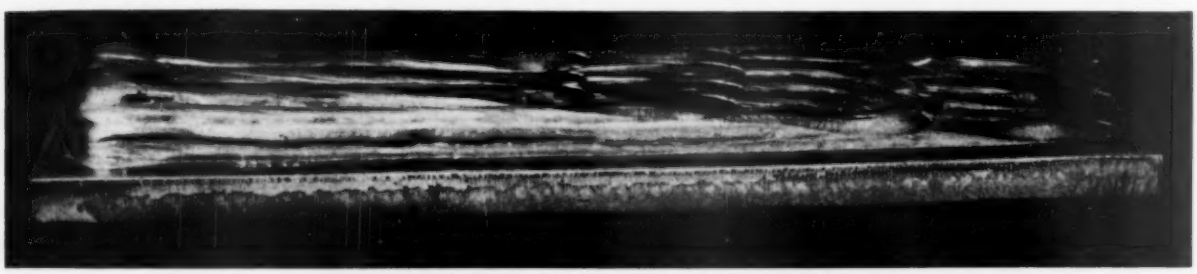

Fig. 1 Multilayer weld pad used for this investigation

Table 1—Influence of Power Supply with "Unionmelt" Grade 88 Welding Composition and Oxweld No. 40 Electrode

Weld No.	Power supply	—Melting rate, lb per min— Electrode	Welding composition	—Weld metal composition of top layer of six-layer deposit, %— C	Mn	Si	Cr	Mo
22328	D-C electrode positive	0.24	0.29	0.084	1.01	0.20	1.30	0.68
22332	D-C electrode negative	0.24	0.28	0.080	0.94	0.20	1.27	0.75
22336	A-C	0.22	0.28	0.096	1.02	0.21	1.23	0.60

used for most of the tests. Grade 88 material is intended to deposit weld metal containing 1.00 to 1.25% chromium, and 0.15 to 0.20% molybdenum when welding with Oxweld No. 36 electrode, containing only residual chromium or molybdenum.

As an initial check on the effect of the type of power source, built-up pads were made using 600 amp, 30 v and 20 ipm travel. With a $^3/_{16}$-in. diam Oxweld No. 40 electrode and Grade 88 welding composition, one built-up pad was welded using direct current with electrode positive, a second using direct current with electrode negative, and the third using alternating current. Each built-up weld pad consisted of six layers deposited on mild steel (Fig. 1). The first layer contained six weld beads deposited side by side; the second, six; the third, five; the fourth, five; the fifth, four; and the final layer contained three weld beads. With this technique, it was felt that dilution with the base metal is reduced to the order of five per cent. The weld metal in the sixth layer of the pad, consisting of three weld beads deposited side by side, was used for chemical analyses and determination of consumption of electrode and welding composition. The results shown in Table 1 indicate that the power source does not influence the results which are obtained with Grade 88 material. Since the melting rates of the electrode and Grade 88 welding compositions are not dependent upon the type of power supply, the uniformity of results is to be expected.

A second series of tests was performed in order to observe the continued uniformity of performance of the welding composition. A 100-lb bag of Grade 88 material selected from production was reduced by quartering to a sample weighing approximately ten pounds. This sample of welding composition was used to make four built-up pads, each pad consisting of 12 weld beads deposited in three layers. The same sample of welding composition was used for the entire series by removing the fused material from the granular material at the completion of each bead and mixing the granular material with the unused portion. The ten-pound sample was reduced by continued use and discarding of the fused portion, so that only sufficient material remained for preparing a proper sample for sizing and chemical analyses. The results shown in Table 2 indicate that little or no change takes place when the unfused welding composition is reused.

PART I—EFFECT OF WELDING TECHNIQUE OF WELD DEPOSIT

Effect of Current on Melting Rate of Electrode

It has been shown repeatedly[3,5] that the melting rate of any electrode is essentially directly proportional to the current. However, most of the data which have been reported relate to carbon steel electrodes with low chromium content.

From a physical standpoint, it can be expected that since chromium-bearing steels generally have higher electrical resistance than mild steel, the melting rate of the chromium-bearing electrode will be higher. In coated electrodes with rather long extension, the I^2R heating, particularly at higher currents, is often sufficient to overheat the electrode. In the submerged-arc welding process, heating in the normally used short extension beyond the contact jaws does not have a significant influence on the melting rate with mild steel; it can have significant effect with the higher chromium steels.

A series of weld tests was made in order to measure the effect of current on the melting rate of a range of electrodes using two grades of material with currents from 250 to 820 amp. The results of these tests are given in Table 3 and shown graphically in Figs. 2 and 3. Additional data presented by Kerr and Elmer[4] have been included. These figures show that the melting rate of chromium-bearing steel electrodes is generally up to 25% higher than that for mild steel electrodes. There is some indication that this increase in melting rate is related to the higher electrical resistance of the electrode, which increases the I^2R heating.

Effect of Chromium Content on Weld Metal Area

A relationship between welding technique and the

Table 2—Uniformity of Results with Continued Use of "Unionmelt" Grade 88 Welding Composition

Weld No.	Pad	—Melting rate, lb per min— Electrode	Welding composition	—Weld metal composition of top layer of three-layer deposit, %— C	Mn	Si	Cr	Mo
22563	First	0.24	0.31	0.074	0.87	0.19	1.14	0.22
22566	Second	0.24	0.30	0.079	0.92	0.17	1.05	0.22
22568	Third	0.23	0.30	0.080	0.93	0.18	1.05	0.21
22572	Fourth	0.24	0.28	0.079	0.93	0.18	1.05	0.21

area of the weld deposit has previously been presented[3] for mild steel base metal with a mild steel electrode. A number of welding tests with electrodes of AISI 430, 317, 310 and 304 compositions were made over a wide range of current and travel. The nugget or weld-metal areas of these welds, together with the tests reported by Kerr and Elmer, are shown in Fig. 4. The line indicating the relationship between welding technique and the area of the nugget is also shown for Unionmelt Grade 20 welding composition with Oxweld No. 36 electrode. The new data for both Grades 80 and 88 welding compositions generally lie along a line parallel to the line for Grade 20 composition with No. 36 electrode. The new line has been shifted to the right about 10%.

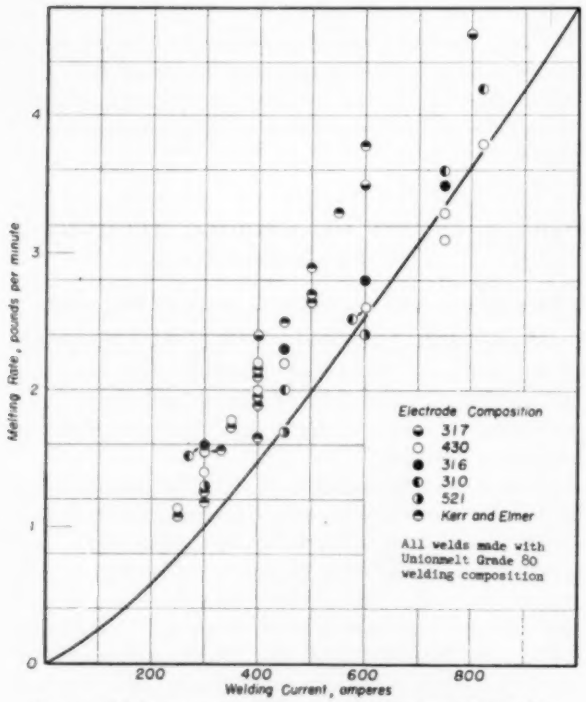

Fig. 2 Melting rate of chromium bearing electrodes

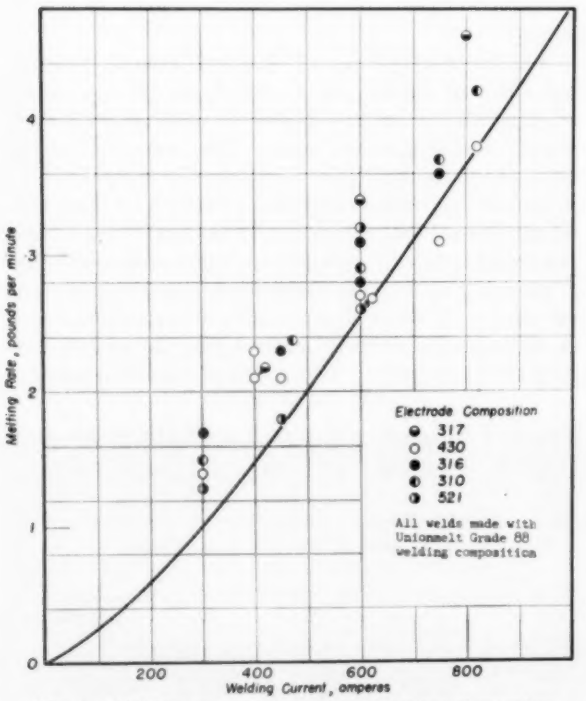

Fig. 3 Melting rate of chromium bearing electrodes

Table 3—Effect of Current on Electrode Melting Rate "Unionmelt" Grade 80 Welding Composition

Weld No.	*Electrode type*	*Welding technique — Amp*	*V*	*Travel, ipm*	*Melting rate electrode, lb./min.*
		Surface beads			
22529-1	430	300	30	20	0.14
3	430	450	30	20	0.22
5	430	600	30	20	0.24
7	430	750	30	20	0.33
9	430	750	30	20	0.31
22533-1	316	300	30	20	0.16
3	316	450	30	20	0.23
5	316	600	30	20	0.28
7	316	750	30	20	0.35
22539-1	310	300	30	20	0.16
3	310	450	30	20	0.20
5	310	600	30	20	0.26
7	310	750	30	20	0.36
22540-1	521	300	30	20	0.13
3	521	450	30	20	0.17
5	521	600	30	20	0.24
		6-Layer pads			
22217	36	600	30	20	0.24
22236	502	600	30	20	0.28
22246	430	600	30	20	0.27
22249	347	600	30	20	0.28
22257	316	600	30	20	0.29
22272	310	600	30	20	0.28
A-4133	521	600	30	20	0.27
		Welds in 1/2-in. thick plates			
22347-1	310	820	30	18	0.42
22349-1	304	820	30	18	0.37
22350-1	430	820	30	18	0.38
22351-1	36	850	28	16	0.36
		"Unionmelt" Grade 88 (Welding Composition)			
		Surface beads			
22529-2	430	300	30	20	0.14
4	430	450	30	20	0.21
6	430	600	30	20	0.27
8	430	750	30	20	0.31
22533-2	316	300	30	20	0.17
4	316	450	30	20	0.23
6	316	600	30	20	0.31
8	316	750	30	20	0.36
22539-2	310	300	30	20	0.15
4	310	450	30	20	0.23
6	310	600	30	20	0.29
8	310	750	30	20	0.37
22540-2	521	300	30	20	0.13
4	521	450	30	20	0.18
6	521	600	30	20	0.26
		6-Layer pads			
22225	40	600	30	20	0.24
22230	502	600	30	20	0.27
22244	430	600	30	20	0.26
22267	310	600	30	20	0.27
22277	316	600	30	20	0.28
22281	347	600	30	20	0.27
22285	521	600	30	20	0.32
		Welds in 1/2-In. thick plates			
22347-2	310	820	30	18	0.43
22349-2	304	820	30	18	0.42
22350-2	430	820	30	18	0.38
22351-2	36	850	28	16	0.37

Table 3 (Contd.)—Kerr and Elmer[4] Surface Beads

Base	Electrode type	—Welding technique— Amp	V	Travel, ipm	Melting rate electrode, lb./min.*
405-A	36	300	30	20	0.13
410-C	36	300	30	20	0.12
405-A	36	400	30	20	0.19
410-C	36	400	30	20	0.17
405-A	36	500	30	20	0.27
410-C	36	500	30	20	0.27
405-A	430B	400	30	4	0.22
410-C	430B	400	30	10	0.20
A-285-A	430B	400	30	20	0.21
A-285-A	430B	400	30	40	0.20
A-285-A	430B	250	30	20	0.11
A-285-A	430B	300	30	20	0.16
A-285-A	430B	350	30	20	0.18
A-285-A	430B	400	30	20	0.21
A-285-A	430B	450	30	20	0.25
A-285-A	430B	500	30	20	0.29
A-285-A	430B	550	30	20	0.33
A-285-A	430B	600	30	20	0.38
A-285-A	430B	650	30	20	0.43

* Melting rate = crown area × travel × 0.28.

This increase in total area is probably associated with the increase in melting rate for the chromium-bearing electrodes.

Effect of Chromium on Penetration

Previous data have been presented showing the relationship between welding technique and penetration.[3] Penetration may be determined by the formula

$$P = K\sqrt[3]{\frac{I^4}{SE^2}}$$

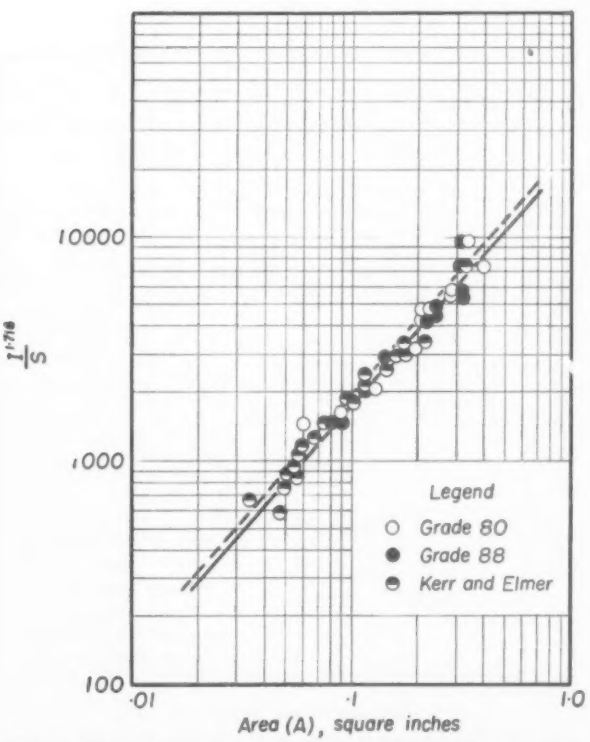

Fig. 4 Effect of current (I) and speed of travel (S) on cross-sectional area (A) of weld

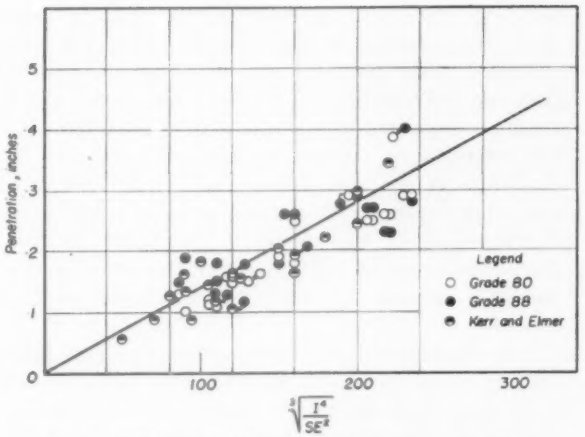

Fig. 5 Effect of welding technique on penetration

where

P = penetration, in.
K = constant depending upon characteristics of welding composition
I = current, amp
S = travel, ipm
E = welding voltage, v

The data for Grades 80 and 88 welding compositions are shown in Fig. 5. A line indicating $K = 0.0014$ has been drawn. Although considerable scatter is to be noted, the penetration cannot be related to the Unionmelt welding composition or chromium content. The data given by Kerr and Elmer for Grade 80 composition and AISI 430 electrode lie in the same scatter band.

PART II—CHEMICAL RELATIONSHIPS

Relation of Grades 80 and 88 Compositions to Chromium Recovery

In the welding of chromium-bearing steels by the submerged arc process, it has been general practice to

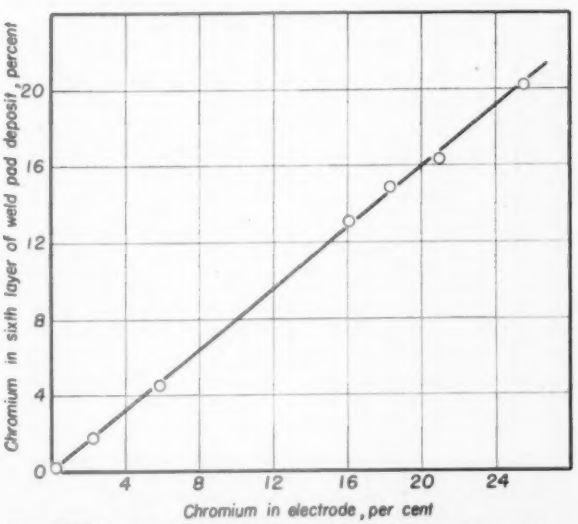

Fig. 6 Relationship of chromium in weld deposit to chromium in electrode Grade 80 welding composition

use an electrode with chromium content slightly higher than that required in the deposit. This has been necessary since there is a loss of chromium from the electrode and from the weld deposit with a corresponding increase in the chromium content of the fused welding composition. Hence, as shown in Fig. 6, the chromium in the weld deposit in a multilayer weld pad using Grade 80 welding composition is 80% that of chromium in the electrode. In usual practice, the chromium content of the electrode is selected so that the weld deposit will have the chromium content desired.

A determination of the chromium content of the fused welding composition for a number of the welds showed an increase in chromium content as the chromium content of the electrode increased. This suggested the possibility of incorporating chromium into a welding composition in order to reduce the tendency for chromium loss from the electrode during welding and to add chromium to the weld. This led to the formulation of Grade 88 welding composition.

Effect of Electrode Composition

The relationship of chromium in the weld deposit to chromium in the electrode is shown in Fig. 6. Seven different electrodes were chosen with chromium contents from 0 to 25%. Welds were made with Grade 80 welding composition. Then tests were made with Grade 88 composition using the same series of electrodes and the results plotted in Fig. 7. In both cases, six-layer weld pads were deposited on mild steel base at 600 amp, dcrp, and 30 v with a speed of 20 ipm. Chemical analyses were made of the last layer of each weld metal deposit to determine the chromium content. From the data in Fig. 7, the presence of the chromium in Grade 88 welding composition has increased the chromium content in the weld deposit, generally by slightly more than 1%. An electrode containing 6% chromium will produce a weld metal pad containing 6% chromium. With less than 6% chromium in the electrode, chromium is transferred from the welding composition to the weld deposit. With more than 6% chromium in the electrode, chromium is transferred to the welding composition.

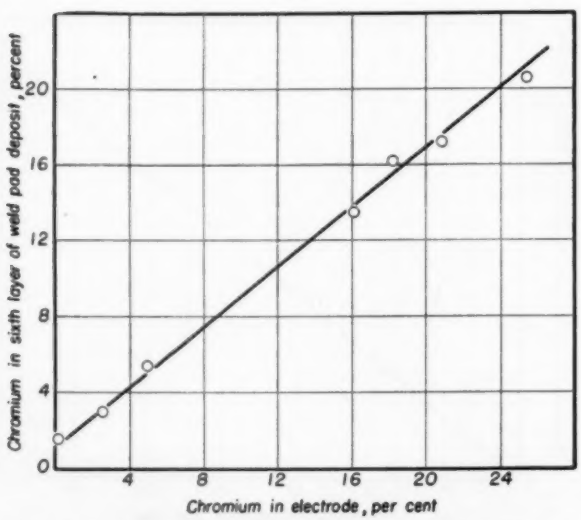

Fig. 7 Relationship of chromium in weld deposit to chromium in electrode Grade 88 welding composition

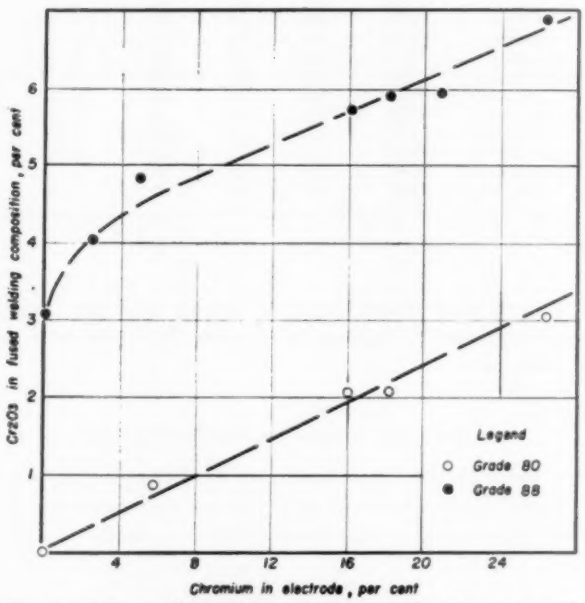

Fig. 8 Relationship between chromium in electrode and Cr_2O_3 in fused welding composition

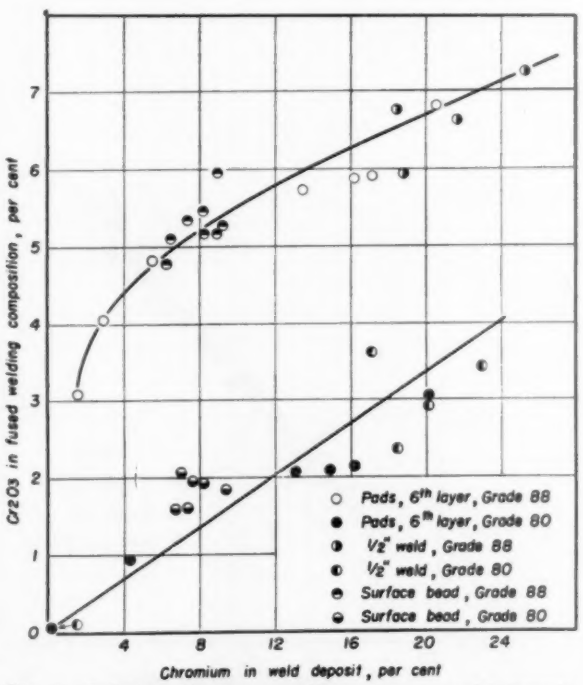

Fig. 9 Relationship between chromium in weld deposit and Cr_2O_3 in fused welding composition for various types of welds

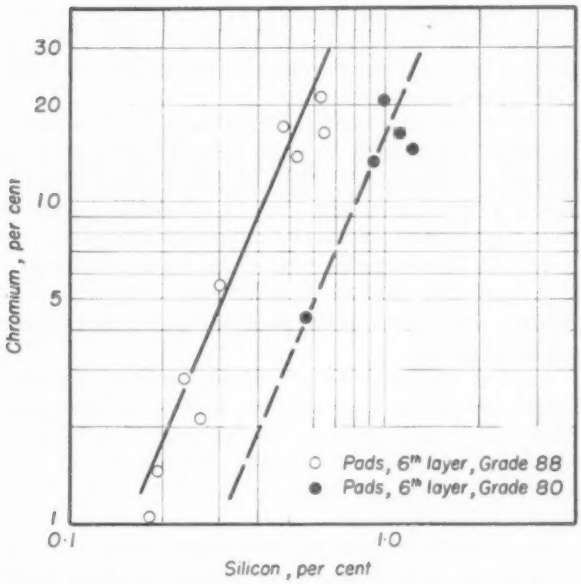

Fig. 10 Relationship between chromium and silicon contents for weld pads made with Grades 80 and 88 welding composition

That there is a partition of chromium between the weld deposit and fused welding composition for both Grade 80 and Grade 88 material can be seen in Figs. 8 and 9, which show a characteristic relationship for each grade. It must be realized that the Cr_2O_3 in the fused welding composition is dependent upon the chromium in the deposit which, in turn, is dependent upon the chromium content of the welding composition, electrode, and base upon which the weld is being deposited.

Effect of Silicon Content

An examination of the chemical composition of the last layer in six-layer built-up pads (Table 4) shows that there is a lower silicon content in all the welds made with Grade 88 material than was noted in the duplicate welds made with Grade 80 composition. As shown in Fig. 10, there is a relationship between the chromium and silicon content such that

$$\frac{\log \text{Cr}}{\log \text{Si}} = K$$

Table 4—Weld Metal Chemical Composition of Sixth Layer in Multilayer Weld Pads

Weld No.	*Elect.**	*Unionmelt grade*	*Weld metal composition, %*							
			C	*Mn*	*Si*	*Cr*	*Ni*	*Mo*	*S*	*P*
22225	40	88	0.06	0.90	0.19	1.49	...	0.62	0.028	0.032
22230	502-A	88	0.08	0.60	0.30	5.51	...	0.61	0.018	0.023
22236	502-B	80	0.05	0.46	0.56	4.48	...	0.50	0.023	0.020
22244	430	88	0.09	1.01	0.52	13.64	0.23	0.20	0.017	0.027
22246		80	0.04	0.83	0.91	13.12	0.20	..	0.016	0.045
22267	310	88	0.10	1.69	0.62	20.61	17.07	0.10	0.021	0.038
22262		80	0.10	1.40	0.99	20.14	16.89	0.06	0.024	0.038
22277	316	88	0.08	2.16	0.64	16.09	12.64	1.85	...	...
22257		80	0.08	1.90	1.21	14.86	12.47	1.70	...	...
22285	521	88	0.07	0.50	0.23	2.86	...	1.09	0.022	0.024
A4133		80	..	..	..	1.87	...	0.83	...	...
22343	36	88	..	1.00	0.21	1.37	...	0.27	...	...
22217	36	80	0.10	1.00	0.60	0.06	...	..	0.026	0.022

* See composition of electrode given in Table 5.

Table 5—1/2-Inch Single-Pass "Unionmelt" Welds

Weld No.	*Elect. (1)*	*Base plate (2)*	*Unionmelts grade*	*Weld metal composition, %*							
				C	*Mn*	*Si*	*Cr*	*Ni*	*Mo*	*S*	*P*
22347-2	310	310	88	0.08	1.39	0.40	25.36	20.12	0.096	0.012	0.016
22347-1			80	0.10	1.35	0.47	23.00	20.76	ND	0.012	0.021
22349-2	304	304	88	0.07	0.97	0.40	21.70	10.40	0.088	0.011	0.018
1			80	0.06	0.97	0.50	20.24	10.42	...	0.014	0.019
22350-2	430	430	88	0.08	0.58	0.24	18.42	...	0.072	0.023	0.014
1			80	0.07	0.53	0.33	17.12	...	...	0.014	0.016
22351-2	36	A212	88	0.11	0.83	0.20	0.24	...	0.076	0.037	0.015
1			80	0.12	0.89	0.33	0.07	...	...	0.037	0.012
(1) Electrode composition:											
310				0.19	1.60	0.44	25.40	19.86	0.04	0.013	0.026
304				0.05	1.97	0.43	20.34	9.87			
430				0.14	0.33	0.45	16.81	0.38			
36				0.15	2.08	0.03	0.13	...	...	0.014	0.017
40				0.14	2.07	0.04	0.19	...	0.48	0.021	0.023
316				0.08	1.85	0.53	18.27	13.85	1.90	...	...
502A				0.12	0.73	0.12	5.05	...	0.56	0.007	0.019
502B				0.04	0.35	0.18	5.99	...	0.59	0.008	0.014
521				0.08	0.36	0.07	2.47	...	0.93	0.011	0.013
(2) Base plate 1/2-in. thickness composition											
310				0.05	1.76	0.37	25.17	20.68	...	0.004	0.013
304				0.07	0.47	0.30	20.74	10.76	...	0.010	0.014
430				0.06	0.45	0.10	17.91	...	...	0.009	0.012
A212				0.16	0.56	0.22	0.09	...	...	0.033	0.007

This relationship appears to be controlled either by the chromium content of the electrode or by the silicon content of the welding composition. This relationship is fairly well established with six-layer weld pads which presumably have attained a fair chromium-silicon equilibrium. In general, the relationship shown in Fig. 10 gives the maximum amount of silicon which is reached in multipass deposits. Singlepass welds given in Table 5 had higher log Cr/log Si ratios, indicating a nonequilibrium condition.

Effect on Alloying Elements Other Than Chromium and Silicon

It appears from the results given in Tables 4 and 5 that the carbon and manganese contents of weld metal are maintained at about the same level by both Grades 80 and 88 welding compositions. There is generally a loss in carbon content of the electrode during transfer and some loss in manganese with electrode compositions such as AISI 310, 316 and 36, that contain more than approximately 0.70% manganese. With electrodes with lower manganese such as Nos. 430 and 521, an increase in the manganese in the weld metal is apparent. There is a gain in molybdenum with the use of Grade 88 welding composition.

SUMMARY

As a result of this investigation of the submerged arc welding of chromium-bearing steels, the following relationships are applicable:

1. The melting rate of chromium-bearing electrodes under normal conditions of welding is somewhat higher than that for mild steel electrodes for both Grades 80 and 88 welding compositions.

2. The cross-sectional area of the weld beads is controlled by the current and speed of travel, and is slightly greater for chromium-bearing electrodes than for mild steel electrodes.

3. The penetration of the weld beads for chromium-bearing electrodes, in general, increases with welding current and decreases with an increase in either speed of travel or welding voltage. This relationship is similar to that previously reported for mild steel electrodes.

4. The chromium content of the top layer of a six-layer weld deposit made with 600-amp welding current is 80% that of the electrode using Grade 80 welding composition.

5. The chromium content of the top layer of a six-layer weld deposit is about 80% that of the electrode plus 1.25% using Grade 88 welding composition. With this welding composition, a balance with no loss of chromium during welding is obtained at 6% chromium in the electrode.

6. At a given chromium level, lower silicon content of weld metal is obtained with Grade 88 than with Grade 80 welding composition.

The use of these relationships will aid in controlling the chemical analyses of the weld deposit which will result in improved performance particularly in applications which require uniform high-temperature and corrosion-resistant properties.

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(*Continued on page 777*)

Spot Welding Thin Aluminum

Slope control with single-phase equipment provides the means of simulating the wave shape of stored energy discharge necessary for welding thin aluminum alloy sheets

by I. W. Johnson

INTRODUCTION AND SUMMARY

THE effect of slope control in spot welding aluminum alloys with emphasis on $^1/_{16}$ in. and greater thickness has previously been reported.* Spot welding thin sheets of aluminum alloys ranging in thickness from 0.016 to 0.040 in. has presented problems that have been difficult to overcome with standard single-phase equipment. Stored energy machines have been satisfactory and for that reason many fabricators have retained such equipment for the sole purpose of welding thin aluminum alloy sheets. It was reasoned, therefore, that if a-c power could simulate the characteristics of stored energy discharge, standard single-phase equipment would become satisfactory.

The up-down slope control provides the means of simulating the wave shape of stored energy discharge. The electronic voltage regulator provides the means of maintaining the magnitude of the current just as precisely as the stored energy equipment. Single-phase spot welders equipped with these two controls are capable of spot welding thin sheets of aluminum to Specification Mil-W-6860. This paper presents data pertinent to the use of "up-down slope control" with electronic voltage regulator on single-phase a-c welding equipment. A summary of these data reveals the following:

1. Spot welds that meet the requirements of Mil-W-6860 can be obtained on single-phase equipment.
2. Crack-free welds can be made in 24ST aluminum without forging pressure.
3. Lower electrode force can be used.
4. With lower electrode force made possible, a substantial reduction in current demand is gained.
5. Phenomenal consistency of tensile shear strengths was observed.

I. W. Johnson is with the Materials and Processes Laboratory, Large Steam Turbine-Generator Department, General Electric Co., Schenectady, N. Y.

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EQUIPMENT AND PROCEDURE

The "up-down slope control" is a supplementary device used in conjunction with standard welder control panels which utilize phase-shift heat control. In addition to the gradual increase of the current, it also permits a gradual decrease of the current after the weld has been completed. This control of the current is accomplished electronically providing precise control of the time. Figure 1 illustrates a typical current trace using such a control.

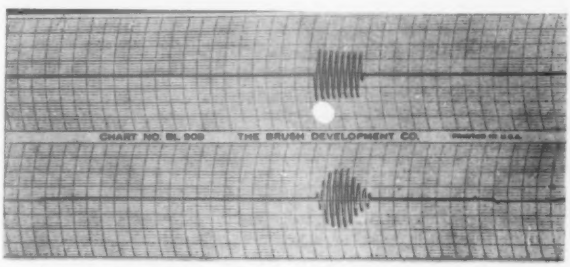

Fig. 1 Voltage trace showing timing of up-down slope. Top trace is of second machine fired to cause a voltage drop of from 10 to 15%

The electronic voltage regulator is an auxiliary control for use with any resistance-welding panel equipped with phase control. The regulator will instantaneously advance or retard the firing point of the phase control unit so that the root-mean-square or heating value of the current is maintained at its preset value regardless of changes in the line voltage.

The electrodes used were Class 1 of the cadmium-silicon-copper type. Previous work has indicated that a 3-in. radius dome is satisfactory. Consequently this was used on all combinations with this exception: when welding 0.016-in. 24ST3 to 0.032-in. 52S, it was necessary to use a flat electrode on the 52S. The cooling water hole was drilled to within $^1/_4$ in. of the welding surface of all electrodes. City water at 58° F circulated through the electrodes at approximately 1 gpm.

The following steps were used to clean the aluminum surface prior to welding:

1. Nonetch type cleaner 170–190° F for 3 to 5 min.
2. Cold water rinse.
3. Deoxidize at room temperature for 3 to 5 min.

Heat-treated stock required 5 min.

4. Cold-water rinse.
5. Conditioner at room temperature for 2 min.
6. Cold water rinse.
7. Deoxidize as in Step 3 for *15 sec.*
8. Cold water rinse.

The surface resistance was measured by clamping two pieces of work between two current-carrying electrodes at a pressure of 1000 lb and reading the resistance on a microhmmeter. The cleaning procedure described produced surface resistances as tabulated in Table 1.

All welding was done on a conventional 50-kva spot welder. The maximum short-circuit secondary current of the machine is 27,000 amp and maximum electrode force 500 lb at 80 lb line pressure.

Table 1—Surface Resistance

Alloy and thickness	*Microhms*
0.016-in. 24ST3	8–32
0.032-in. 24ST3	20–46
0.020-in. 52S	8–12
0.032-in. 52S	8–16
0.040-in. 61ST4	10–40

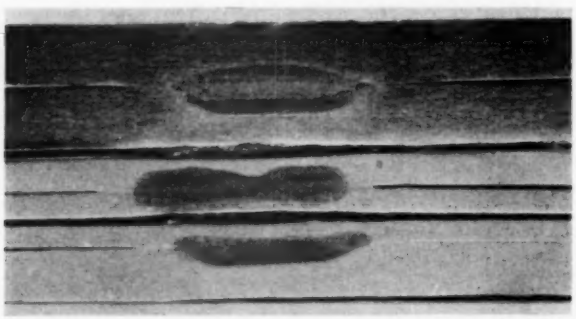

Fig. 2 Typical cross sections

Top to bottom: 0.032-in. 24ST3 to 0.032-in. 24ST3, 0.016-in. 24ST3 to 0.020-in. 52S, 0.016-in. 24ST3 to 0.032-in. 52S.

A second spot welder was interconnected through the initiating circuits so that it could be fired simultaneously with the machine making the test welds. This machine was set to draw approximately 800 amp in the primary causing a drop in voltage of 10 to 15% as indicated by a Brush Recorder.

In all the tests Mil-W-6860 specification was used as a guide to determine weld quality and strength. Cross section and X-ray examinations were made to determine the soundness of the spot welds. Figure 2 illustrates typical cross sections and Fig. 3 a typical X-ray.

An additional test known as a cross-section bend test is illustrated in Fig. 4. The specimen is cut so that the center of the weld is located on the surface to be bent. This surface is etched before bending to permit bending the specimen with the center of the weld at the point of maximum stress. After bending 45 deg, no cracks should appear in the weld or base metal adjacent to the weld.

RESULTS AND DISCUSSION

Welding procedures are tabulated in Table 2. The procedures produced welds that were free from internal defects and of a strength that met the requirements of Mil-W-6860. The surface appearance was smooth and without evidence of burning or pitting.

The initial purpose of this investigation was to determine if by using up-and-down slope control in conjunction with the electronic voltage regulator, spot welds in thin aluminum could be made that meet the requirements of Mil-W-6860. Early in the investigation it became evident that this equipment possessed further

Fig. 3 Typical X-ray showing a portion of test panel from 0.016-in. 24ST3 to 0.032-in. 52S

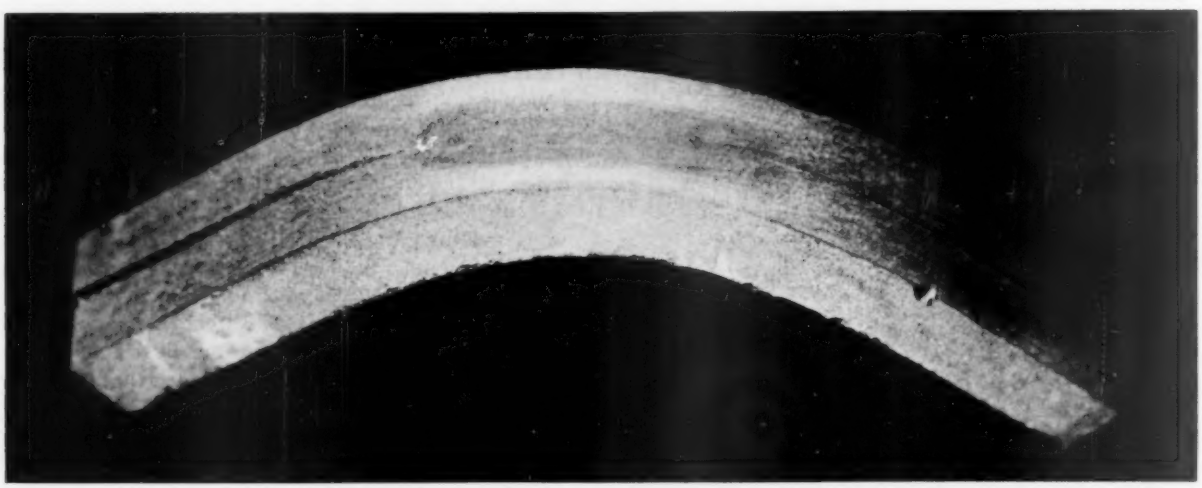

Fig. 4 Cross-section bend test showing outside of bend. This section was bent 45 deg around center of weld. ×8

Table 2—Spot-Welding Schedules for Aluminum Alloys

Alloy and thickness (in.)		Time, cycles: Up	Weld	Down	Electrode force, lb	Weld current, amp	Spot strength, lb
0.016 24ST	0.016 24ST	3	3	6	320	20,000	240
0.032 24ST	0.032 24ST	4	3	13	400	21,000	440
0.020 52S	0.020 52S	3	3	3	320	22,000	210
0.032 52S	0.032 52S	3	3	6	400	23,000	400
0.040 61ST	0.040 61ST	3	3	13	400	24,000	490
0.016 24ST	0.032 24ST	3	3	6	320	20,000	250
0.016 24ST	0.020 52S	3	3	6	300	21,000	210
0.016 24ST	0.032 52S	3	3	6	300	21,000	260

merit. It was soon obvious that a low power level could be used satisfactorily. Lower electrode force is permissible when using lower current. Normally low current introduces inconsistencies in spot strength. The voltage regulator removes this inconsistency and accomplishes welds of remarkable consistency. Table 3 summarizes the results when welding with and without the slope control or voltage regulator. In this summary it may be noted that sound welds of consistent strength was always accomplished when using both slope control and regulator. Without the slope-control cracking, expulsion and sticking to electrodes occurred. Without the regulator inconsistency in strength was evidenced.

The outstanding consistency was observed when welding two pieces of 0.020-in. 52S aluminum. The first test run consisted of 10 welds. All welds broke at 210 lb. The next test consisted of 25 welds. Before welding, each set of specimens was checked for surface resistance at the point the weld was to be made. Some specimens of relatively high surface resistance were included. The resistance varied from 8 to 50 microhms. Again all specimens revealed a strength of 210 lb. To determine the repeatability of this procedure, it was returned to at weekly intervals for 5 weeks. Each run consisted of 10 welds. The tensile-shear strength of these 5 groups revealed a low of 205 lb and a high of 215 lb. It is believed that this truely represents one optimum procedure.

The more serious problems encountered when spot welding aluminum may be listed as follows:

1. Inconsistent strength.
2. Expulsion of metal and material defects.
3. Surface burning or pitting.
4. Excessive sheet separation.
5. Excessive indentation.

Table 3—Summary of Results When Spot Welding Aluminum with and Without Slope Control and Regulator

Equipment: Slope control	Regulator	2nd machine	Strength: High	Low	Remarks—Condition of weld
		0.016-in. 24ST to 0.016-in. 24ST			
Yes	Yes	No	260	240	Sound
Yes	Yes	Yes	250	225	Sound
Yes	No	Yes	240	140	Inconsistent strength
No	Yes	No	260	225	Cracks—expulsion
No	Yes	Yes	250	215	Cracks
No	No	No	250	220	Cracks—sticking
No	No	Yes	240	125	Cracks—inconsistent strength
		0.032-in. 24ST to 0.032-in. 24ST			
Yes	Yes	No	455	420	Sound
Yes	Yes	Yes	460	420	Sound
Yes	No	Yes	445	325	Inconsistent strength
No	Yes	No	460	420	Cracks
No	Yes	Yes	460	420	Cracks
No	No	No	490	350	Cracks—inconsistent strength
No	No	Yes	440	310	Cracks—inconsistent strength
		0.020-in. 52S to 0.020-in. 52S			
Yes	Yes	No	210	210	Sound
Yes	Yes	Yes	210	210	Sound
Yes	No	Yes	190	130	Inconsistent
No	Yes	Yes	210	180	Expulsion
No	No	Yes	190	110	Inconsistent-expulsion
		0.032-in. 52S to 0.032-in. 52S			
Yes	Yes	No	430	400	Sound
Yes	Yes	Yes	410	380	Sound
Yes	No	Yes	350	230	Inconsistent-low
No	Yes	No	410	375	Expulsion
No	Yes	Yes	410	380	Sticking and expulsion
No	No	No	415	375	Expulsion
No	No	Yes	400	300	Expulsion
		0.040-in. 61ST4 to 0.040-in. 61ST4			
Yes	Yes	No	500	480	Sound
Yes	Yes	Yes	490	465	Sound
Yes	No	Yes	380	310	Inconsistent—low
No	Yes	No	490	440	Expulsion
No	Yes	Yes	475	420	Expulsion
No	No	Yes	400	290	Inconsistent—expulsion

Inconsistency in spot strength may be attributed to improper surface conditions or variation in time, force or current. Rigid control of cleaning procedures and care in handling between cleaning and welding will assure satisfactory surface conditions. Precision timing is obtained in the slope control and only periodic calibration is necessary to confirm its dependability. Proper maintenance of the welder will assure constant and dependable electrode force. The current is the only remaining variable to control. The electronic voltage regulator operates instantaneously to correct for any line voltage fluctuations and thus assures constant rms current or heating value.

Metal expulsion occurring between sheets can be attributed to high surface resistance or high current density before intimate contact is reached. The up-slope time will permit intimate contact to be reached before full welding current and will, therefore, minimize expulsion except in cases of severe oxide or foreign material at the point of weld. The down-slope time eliminates the severe quenching at the end of the weld and the subsequent thermal stress introduced in the weld zone. Thus, internal cracking of the weld is avoided.

Surface burning or pitting may be caused by high surface resistance or high current density before the electrodes have become properly seated. The up-slope time permits the electrodes to become firmly seated before welding current is reached and burning or pitting is eliminated.

Excessive indentation can be attributed to excessive pressure, improper electrode contour or improper forging time. The up slope eliminates the necessity of high pressure and the down slope removes the necessity of forging pressures. If the electrodes are properly machined and maintained excessive indentation should not be a problem. Sheet separation is also caused by excessive pressures. Therefore, the lower pressures permitted when using the slope control will permit work to remain in closer contact. Too long a weld time also causes sheet separation. The down slope makes it unnecessary to use long weld time.

It has been demonstrated that the up-down slope control will remove or minimize many difficulties in spot welding aluminum. It has further been proved that the electronic voltage regulator assures consistent weld strength even at low power level. It may be concluded, therefore, that by normal care and attention to all factors, quality spot welds in aluminum can easily be attained on production. Fabricators of aluminum can be assured of quality welds at reduced cost by proper application of slope control and electronic voltage regulator.

Resistance Welding in Jet Engine Manufacturing

Growth of resistance welding and its uses at Pratt & Whitney Aircraft covering: the principles of resistance welding, equipment, part design and materials, quality control and inspection, and tools

by F. J. Wallace

THE principle of resistance welding has been known for some 50 years. Elihu Thomson, in 1877, discovered that by reversing a current in his electrical apparatus two wires could be fused. After further experiments and improvements, he applied for a patent in 1886. Twelve years later, the first spot welders were in American industry. As can be seen in Fig. 1, the growth of the resistance welding industry was comparatively slow until 1933, when 2.5 million dollars worth of equipment was manufactured. From this point, the industry expanded rapidly until 1945, when the year's production was valued at some 30 million dollars. Production tapered off after that date and is now on the rise because of the use of light metals and alloys in the commercial and aviation field which can be readily joined by resistance welding.

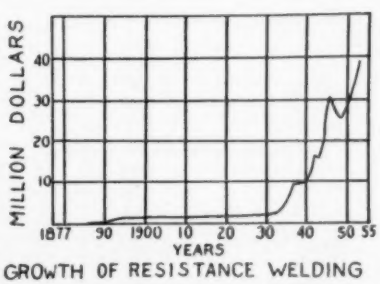

Fig. 1 Growth of resistance welding

Resistance welding was never of major concern in the aircraft (power plant) industry until the advent of the jet engine. In the transition period from radial to jet, resistance welding increase from 1000 to 2000%. As an example, a typical jet engine involves approximately 2.5% of resistance welding time, e.g., of the total time required to build a jet engine, whereas one with an afterburner requires approximately 4.5%. The resistance welding required for a radial engine is in the order of 0.002%. In the face of such a tremendous increase, the industry set itself in gear to meet the challenge; con-

F. J. Wallace is Supervisor, Welding Processes, Pratt & Whitney Aircraft, East Hartford, Conn.

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sequently, the personnel and equipment used for radial engine production was wholly inadequate for the jet engine programs to come.

The equipment which was used for radial engines was an alternating current, single-phase, and single-impulse type of welder. The kva ratings of these machines ran from 40 to 75 which at a 50% duty cycle would deliver from 5000 to 40,000 secondary amperes. A range from 100 to 2000 lb weld force was possible from the pneumatic system used on this type of machine.

The only parts welded in any quantity were aluminum baffles made from sheet stock 0.031 to 0.062 in. in thickness. Welding consisted of spot welding stiffeners to a preformed baffle to prevent bending and tearing in engine operation As these parts were not subjected to extreme temperatures, thermal stresses did not enter into the design, therefore, welding was performed to Class B specifications. These specifications require that the machines be qualified for the metals to be welded. For a given metal, 25 samples of the minimum and 25 samples of the maximum thicknesses are welded and subjected to macroetch, shear test and examined further for indentation surface pickup, etc. The minimum and maximum limits of qualification are determined by the thickness range of the material to be welded in production. Upon acceptance of the qualification, the machine is ready for production parts.

Class B welding specifications also require sample test joints prior to, during and after a production run of parts. These test joints consist of three single-spot specimens on a given overlap to be used for a shear test and five single-spot specimens on a given overlap to be used as a consistency test. The three-spot shear test sample is required every hour during a production run and also after various interruptions.

As a further control of quality, the welders are given an electrical check once a month. This check consists of checking the timing circuits, the pressure system, and cleaning the secondary contacts.

The welded parts are inspected usually for appearance, indentation, and number and spacing of spots.

JET ENGINE WELDING EQUIPMENT

Welding equipment which was found to be necessary for the manufacturing of jet engines was of two basic types—portable and stationary, e.g., press or rocker arm. Portable equipment is used to tack weld individual detail parts or subassemblies which are positioned or held in jigs and fixtures. Jigs and fixtures have two basic functions. One is to locate one detail with respect to another for dimensional control while the second is to permit greater ease in handling a heavy or awkward subassembly. Many of the fixtures used in tack welding of large parts are too heavy to move. Therefore, the portable equipment is quite advantageous as the electrodes can be moved to the work. Typical applications of portable equipment are shown in Fig. 2.

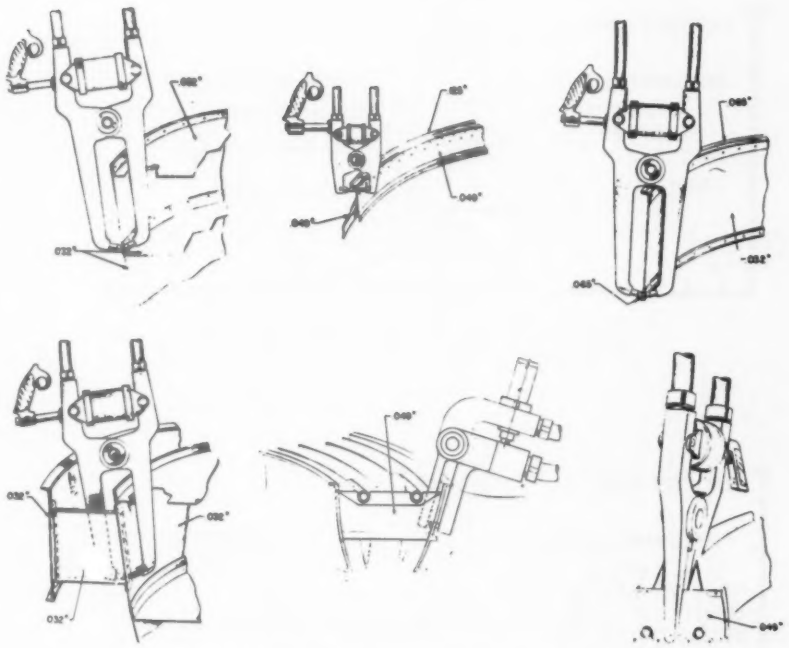


Fig. 2 Application of portable welding guns

Portable equipment has about the same features as the equipment used for radial engine welding, although the pressure range is much less because of the deflection encountered in using the C-type frame. The maximum pressure used is about 800 lb, and for this reason quality welds are not attempted on this equipment. A quality weld is one which must meet PWA Specifications for soundness, penetration, nugget diameter, shear strength and indentation.

Equipment used for quality welding on the assemblies and subassemblies of the jet engine was greatly improved over the equipment used for radial engine welding. The improvements were not only in the control of variables and added features, but also improvement of power supply balance. The power factor for a single-phase machine is 30%, whereas three-phase (low-frequency converter) machines, now used, have a power factor of 80–90%. Utilization of three-phase equipment does not unbalance the power supply as does single-phase equipment.

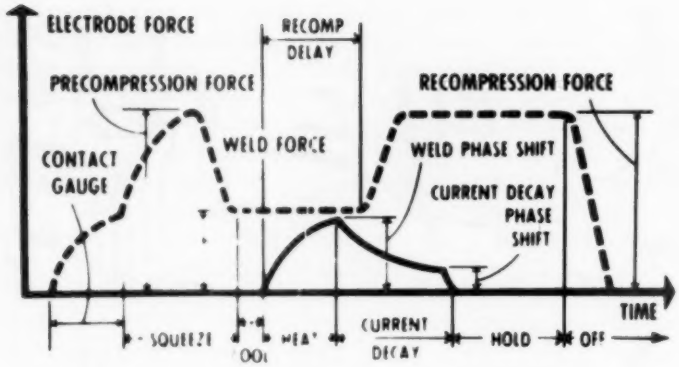
Where the older machines (1940–48) had a single impulse of current, the new ones (1949–53) have anywhere from 1 to 10 impulses. Through the use of synchronous timers for the control of "off" and "on" intervals of current flow, several impulses of current may be used which are accurate to one cycle ($^1/_{60}$ sec). Current range of this equipment is from 5000 to 60,000 amp with the kva rating from 50 to 100 at 50% duty cycle. A two-stage pneumatic pressure system permits a weld schedule with variable pressure and a timer to control the point in the weld sequence for high-pressure application. This is shown in Fig. 3. The pressure range of this equipment is from 100 to 4500 lb.

As for some of the added features, the latest machines in use include timers for preheat, quench, postheat and current decay. Some of these variables are illustrated in Fig. 3.

Figure 4 shows a new type seam welder. This machine does not have preheat or postheat as these features are only on the spot welders. It does, however, have intermittent drive which allows current to flow only while the wheels are stopped. This is followed by an interval of "hold time." The wheels then index for the next spot, at which time the weld interval is initiated and the sequence repeated. During the weld interval several impulses of current may be used. Each interval of time—i.e., weld time, forge or hold time, motor run time (wheels in motion)—has its own timing controls to make possible various speeds and spots per inch.

PARTS AND METALS

A jet engine is made up of the compressor section, combustion section, turbine section, tailpipe and/or afterburner as shown in Fig. 5. Shown also are some of the metals which go into the various sections. These will be more fully dis-



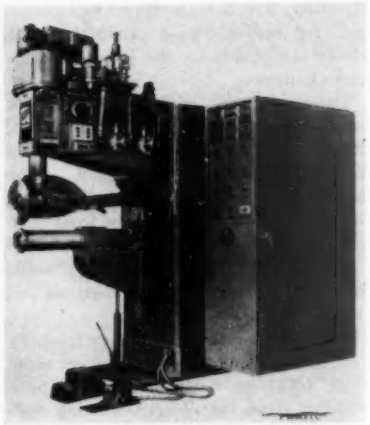


Fig. 4 New type seam welder

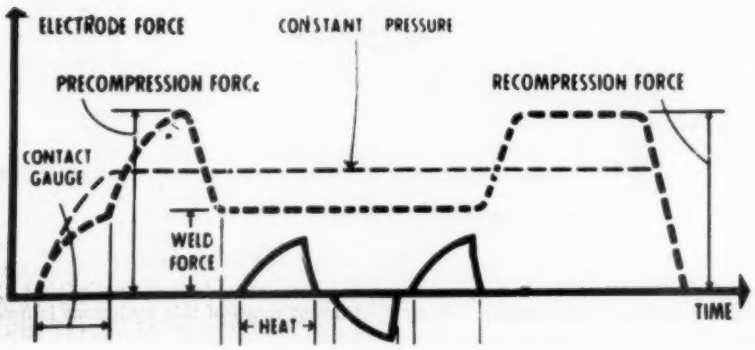


Fig. 3 Graphic representation of spot-welding sequence

cussed later. The welded components of the jet engine are made from sheet stock, forgings and castings. Rigidity is given to these components by flanges and stiffeners which are spot, stitch and seam welded to the sheet-metal sections. Castings and forgings machined to a particular shape make up the flanges. Figure 6 illustrates some of the shapes used.

A combustion chamber is shown in Fig. 7. This is a typical jet engine part. The various sections of the chamber have patterns, the shape of which, are scribed on sheet metal of the proper composition and thickness. After cutting, the details are formed, tack welded and seam welded. A group of mating sections are placed in locating fixtures and tack welded, followed by seam welding. They now become subassembly. One subassembly is then seam welded to another and the main body of the skin is formed as shown in Fig. 8. This, however, happens to be a tail pipe assembly.

On certain portions of the skin, under stress, where the chamber will be joined to other assemblies, flanged tubes are tack welded into place and stitch welded on press-type spot welders.

In many of the afterburner parts, ducts which form the skin, are stiffened by the use of flanges, as were shown in Fig. 6. In this type of operation again the flanges are located in a fixture and tack welded to the sheet metal skin. This is followed by seam welding. The diameter of these ducts runs as large as 3.5 ft. Afterburner parts also have a variety of stiffeners, which are spot and stitch welded to the skin of the ducts.

The metals used for the various components of the jet engine require the ability of retaining their "room temperature" properties at elevated temperatures. Metals used are austenitic and martensitic stainless steels, low-alloy steels, and nickel and cobalt base alloys. The resistance weldability of these materials was found to be good, although some problems were encountered from their metallurgical characteristics, they were ultimately corrected by alloy addition to the basic metals and to machine schedules, surface finishes and equipment variables.

Austenitic stainless is very susceptible to carbide precipitation in the temperature range from approximately 800 to 1600° F. This condition can cause intergranular corrosion which under some conditions may result in failure. To prevent carbide precipitation, a weld schedule of as short a weld time as possible, is used. Additions of titanium, tantalum and columbium to the base metal tend to stabilize the carbon so that little or no carbide precipitation takes place during welding.

It has also been found that the rate of heat input with respect to the physical properties of a given metal has an effect upon its weldability. As an example, aluminum which has very high thermal conductivity and low resistance requires a

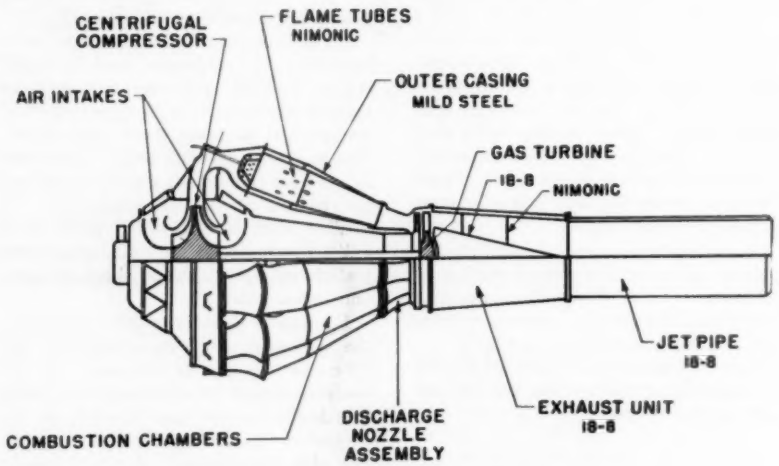


Fig. 5 Layout of typical jet engine

high rate of heat input, e.g., short weld time–high current, whereas nickel base alloys with a much lower thermal conductivity and higher resistance require a slower rate heat, e.g., longer weld time–low current, or overheating will occur.

At the present time we are developing welding techniques for titanium and its alloys, which we expect to be used in greater quantities in the near future.

QUALITY CONTROL

When a new resistance welding machine is received, it first must be qualified electrically. This process consists of checking the various timing circuits for cycling and consistency, checking the pressure system, and checking the short circuit secondary current against nameplate data. The equipment used for this check is shown in Fig. 9. Illustrated are Bru[illegible] oscillograph equipment, strain gage and toroidal coil. A graph of phase shift dial reading versus secondary current is plotted and is posted on the machine for reference when establishing a weld schedule. When the electrical check has been completed, the machine is turned over to a weld technician for metals qualification.

For a given metal, 25 samples of the minimum thickness and 25 samples of the maximum thickness are submitted to the weld inspection department for examination to our own inspection specifications. These specifications state that the indentation shall not exceed 10% of the total actual thickness of a two-thickness weld, or 10% of the total actual thickness of the two outside pieces of a three-or-greater-thickness weld. The minimum penetration in either sheet may not be less than 20% of the indented thickness of the thinnest sheet and the maximum penetration may be no greater than 80% the indented thickness of the respective sheets. In the case of a weld of greater than two thicknesses, the control applies to the two outer pieces.

In testing for shear strength, 20 samples are used. The specification states that no sample may exceed the average strength (20) by more than ±20% and that no more than two samples may exceed the average strength by more than ±10%. The diameter of the weld nugget shall be the same or greater than the minimum nugget diameter for the given welded thicknesses.

When the minimum and maximum samples are accepted for the given material, the welder is qualified for a particular group of metals, i.e., austenitic stainless steel, martensitic stainless steel, nickel base alloys or low-alloy steels. Each group has a particular metal which is representative of this group and when the welder is qualified with this material, any part made of any other metal in that group may be welded. The minimum and maximum thickness are determined by the welding committee from the parts made of the particular material. When samples are submitted for examination, both minimum and maximum thicknesses are welded to themselves. Once qualified, the thinnest sheet in any combination of like material must fall between the minimum and maximum qualified values of that material. There is no limit on the thickest member of the combination. When welding dissimilar metals in any combination, the minimum thickness of each material must fall between the respective minimum and maximum qualified values. As long as the above requirement is met, there is no limit to the maximum thickness of either material that may be used in the particular combination.

Now the machine is ready to be turned over to the production department. Before a production run can be made, however, a weld schedule must be developed. This is accomplished by preparing five sample test joints which simulate the parts to be welded. The five test joints are inspected to the same standards as the qualification samples except the shear strength is not subject to a percentage control. When the weld schedule has been established, a production run is started by a single-test joint and, in addition, one is required every 2 hr during the run. When the run has been completed a final test joint is again submitted for examination.

During the production run, if the original or one of the regular 2 hr tests should be rejected by the weld inspection department, production is stopped and the job is turned back to a weld technician. His job is to analyze the trouble from the weld test results and from visual inspection. He may have the electrical department check the machine to locate trouble

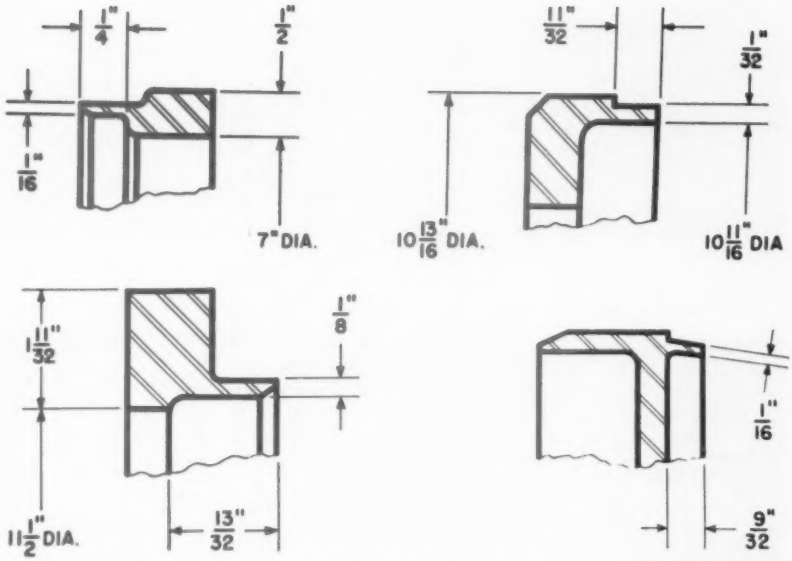


Fig. 6 Various cross sections showing shapes of flanges used for stiffeners

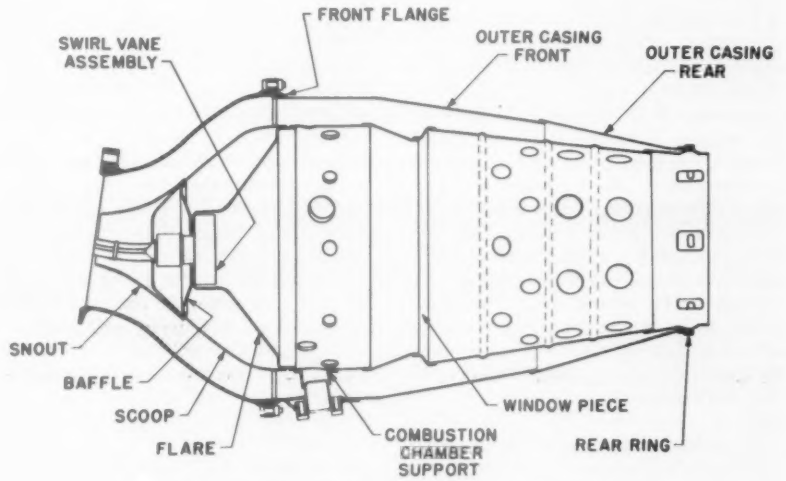


Fig. 7 Cross-sectional diagram of combustion chamber

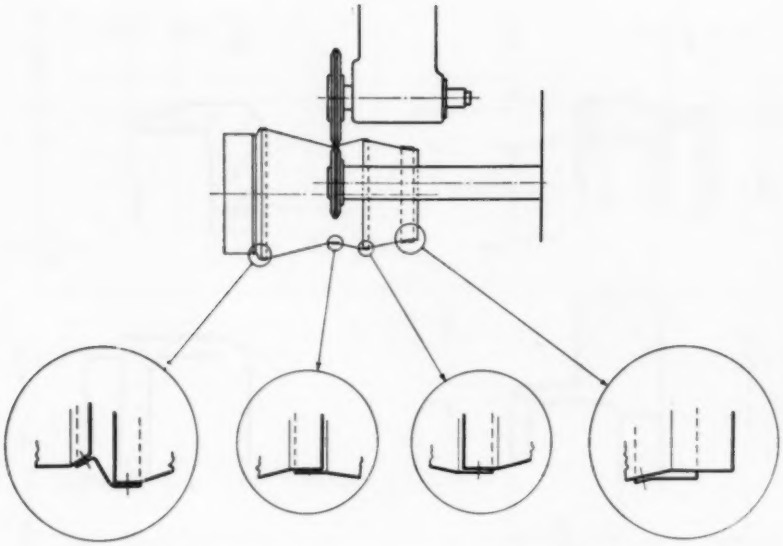


Fig. 8 Welding flame tube sections nimonic 0.064 to 0.064 in., max

Fig. 9 Equipment used in analyzing characteristics of spot welder

or he may decide that a new weld schedule is required. Prior to the establishment of a weld schedule, samples similar to those in Figs. 10, 11 and 12 are examined by the technician. When he is satisfied that the samples meet the standards in all respects, five samples are again submitted for examination. With acceptance of this schedule, production is resumed.

On any welder, when a schedule has been established, a setting chart is made out by the technician and posted on the machine for future reference. This chart is used to set the machine by Production when the job is run again and is used also by the inspection department to see to it that the machine controls are set accordingly.

To keep the welding machines in proper running condition, the electrical department performs a monthly check of all machines, e.g., preventative maintenance. The timing circuits which include squeeze, hold, preheat, weld, quench, postheat, pressure delay, heat cool, and current decay are checked for cycling. Certain of these timers are synchronous which means there can be no deviation from the dial setting. These timers have to be checked very carefully. The remainder of the timers are nonsynchronous and do not require such accurate checking. In addition to checking timers, the current output of the machine is checked on the low and high limits. The pressure system also receives a monthly check. Each type of pressure (high, low and variable) is checked to ascertain if the operation is correct and the calibration of the indicating dial is checked for its accuracy.

Every three months the cleaning of the secondary contacts is performed on the welding equipment. As the copper corrodes, the oxide formed increases the resistance of the secondary circuit. When a welding schedule is developed, for each production run thereafter every control must be identical to the original setting, the current magnitude being the only variable. This may be varied $\pm 10\%$ of the dial setting. When the secondary contacts are not cleaned each month, it is quite possible that the current setting may have to be set above the $\pm 10\%$ range in order to give the same secondary current as the original weld schedule.

Once every year, the upper and lower arms are torn down, the contact bearings are cleaned and checked for wear, etc.

By a careful control of these preventative maintenance measures, lost time of production due to machine breakdown is kept to an absolute minimum along with the cost of operation.

In addition to the above-mentioned measures for quality control, a welding development group exist whose function it is to develop new welding techniques and to aid the welding technician when he has neither the time nor equipment to do a particular job. An area has been set aside for this work where ample welding and laboratory equipment to work with is provided.

INSPECTION

Final inspection after welding of the parts for the jet engine is about the same as for those of the radial engine. Indentation, appearance, sheet separation, and weld location are the main factors to be checked at this time. However, X-ray is extensively employed as an additional means of quality control. A more thorough inspection is felt unnecessary considering the extent of quality control prior to and during the production run.

The percentage of parts X-rayed in the early stages of jet engine manufacturing was higher than at the present time. This lower percentage indicates the progress of quality control measures. A sampling plan controls the reduction in X-ray requirements.

TOOLS

The electrodes that are used for the resistance welding of jet parts are all made from copper base alloys. An RWMA Class 3 material is used more than any other type although Class 1 or Class 2 electrodes are used for some jobs. Electrode life is better using Class 3 material because of its greater hardness. Also the high-temperature properties of Class 3 material are better than either the Class 1 or Class 2 materials.

Jigs and fixtures used with the jet engine parts are designed for the location of component parts, e.g., dimensional control and for locating the electrodes in the correct position. They must be light in weight, corrosion resistant, nonmagnetic and insulated from the welding current. In general, the design is kept as simple as possible. For certain jobs, where a stitch weld is made in a circular path in the horizontal plane, an automatic indexing head replaces the upper electrode assembly. The resulting weld is made faster and is more uniform than the stitch weld made by the manual operation. All that the operator does is to load the machine, press a button to start the sequence. The complete sequence is automatic. The head comes down, welding starts welding stops at the correct moment, the head retracts and the operator unloads the machine. Only a few of the automatic indexing heads are used but their application has increased production of certain parts. Further application of this type of tool is now being considered to increase present production on as-

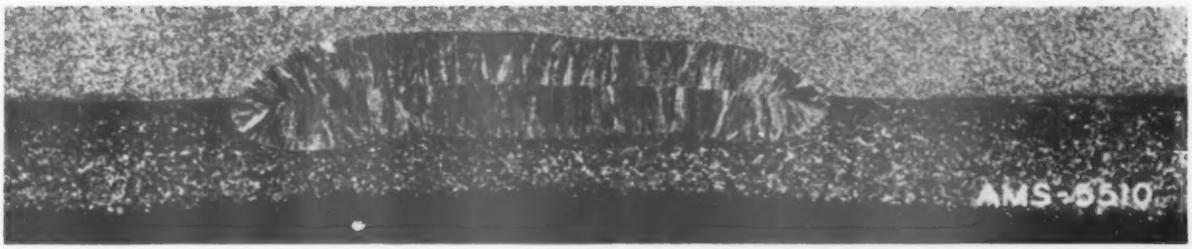


Fig. 10 Spot weld of nickel base alloy, 0.042 in., to austenitic stainless steel, 0.031 in.

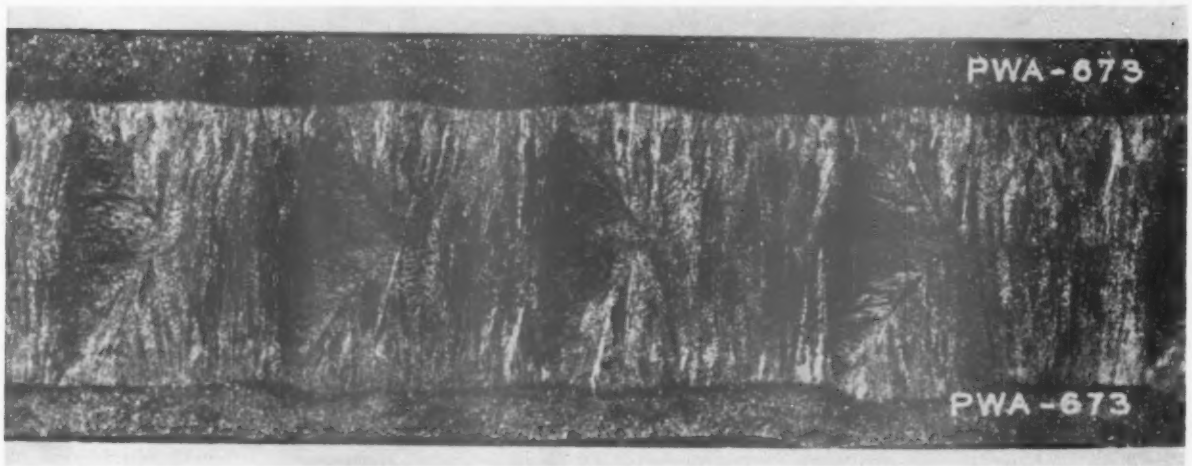


Fig. 11 Stitch of nickel base alloy to itself, 0.062 to 0.062 in.

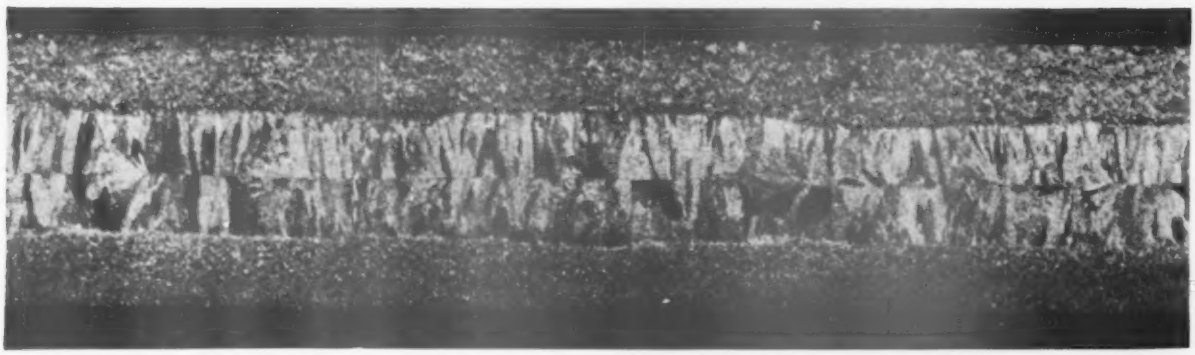


Fig. 12 Stitch weld of nickel base alloy, 0.042 in., to austenitic stainless steel, 0.042 in.

semblies whose configuration is other than perfectly flat.

CONCLUSIONS

The application of resistance welding to the manufacturing of jet engines has made it possible to mass produce many complicated shapes at a reasonable cost. The rapid technological advancements would not have been possible had it not been for the cooperation of the aircraft engine and resistance-welding industries. In the next few years, if the present rate of advancement continues, resistance welding will play an even greater role in the manufacturing of jet, rocket and atomic engines.

Correction Notice

In the article by John Mikulak and Merle Dillman appearing on page 449 of the May issue, in the subtitle of Fig. 1 the author advises that an error was made in transmission of the manuscript and this formula should read:

$$\text{Theoretical throat} = V \times \text{Co-sine}\left(\text{Tan.}^{-1}\frac{V}{H}\right)$$

instead of that shown, which reads

$$V \times \text{sine}\left(\text{Tan.}^{-1}\frac{V}{H}\right)$$

High Production Applications of Resistance Welding

The steady increased use of resistance welding as a production tool is traced together with its impact on the high production of ranges, dryers

by C. D. Shultheis

WE ARE all familiar with resistance welding as a tool in modern high production industry, but do we all realize just how important a tool it is. Just what changes would be required if this tool were not available? Or, where would our industry and our economy be today if this tool had never been invented? By studying a few applications of this tool at Frigidaire perhaps we can arrive at an approach to some answers.

First of all let's go back into history a little and see what has happened at Frigidaire since resistance welding was introduced back in 1933—21 years ago. Figure 1 is a graph showing the growth of welding and production at Plants 2 and 3 where the bulk of home appliances are produced. We started in 1933 with just 34 standard welders used on refrigerator and ice cream cabinet production. By 1937, when a line of electric ranges was added, 180 welders were used. The average welder was only slightly over 100 kva, for they were predominately standard spot and gun welders. By 1947, when our line included refrigerators, ice cream cabinet, food freezers, ranges, washers and water heaters, the number of welders increased to 270 with a total kva name plate rating of 37,000. The percentage of large special welding machines increased correspondingly.

Our production and our equipment continued to increase at such a rate that in 1951, Plant 3 was opened to increase our capacity beyond that available in Plant 2 alone. The move nearly doubled the floor space, allowing for increased facilities for refrigerators, ice cream cabinets and food freezers at Plant 2 and for ranges, washers, water heaters and a new dryer at Plant 3. So in 1953, Plant 2 used 278 welders with a total kva name plate rating of 56,800 and Plant 3 used 205 welders with a total kva name plate rating of 28,400.

Looking back again into history, we see in Fig. 2 a sketch of the 1932 refrigerator. The cabinet frame was of wood with metal panels screwed in place. Welding was still untried as a production tool. In 1933, the refrigerator shell and liner first used some welding, but on a limited scale. However, the 1954 refrigerator as seen in Fig. 3 has extensive resistance welding in every major assembly.

The story of our electric range is much the same although we did not start making them until 1937. Figure

C. D. Shultheis, Welding Engineer at Frigidaire Division Plant 3, General Motors Corp.

Presented to the Dayton Section of the AMERICAN WELDING SOCIETY, Mar. 9, 1954.

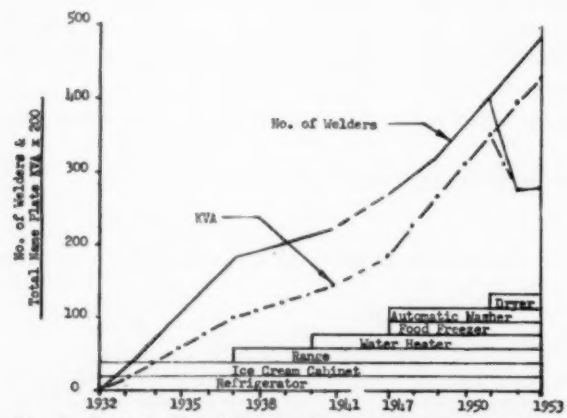


Fig. 1 Growth of welding and production at Frigidaire Plants 2 and 3

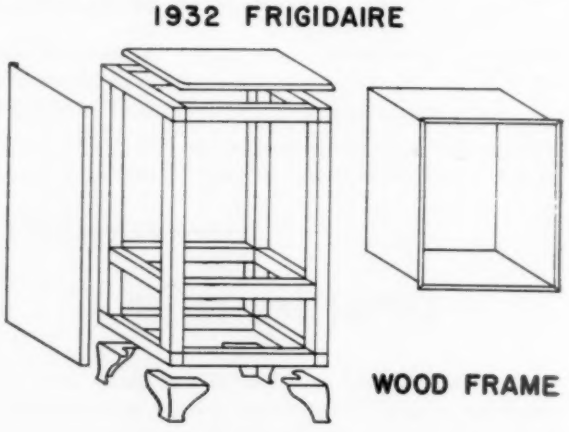
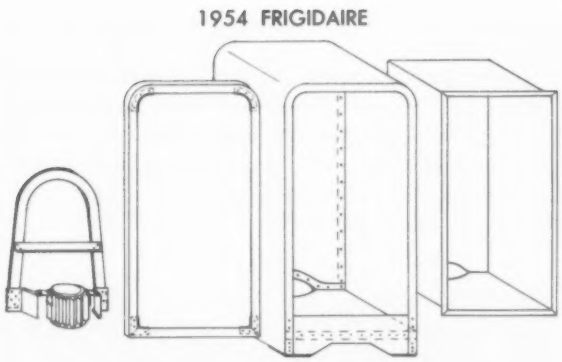


Figure 2



COMPLETELY WELDED
Figure 3

Figure 4

4 is a photograph of one of our first model ranges. Welding was confined to simple spot welding on the frame, top, oven and drawer. All welding was done on standard spot and gun welders using simple fixtures and production was very low at the start.

But let's take a look at the 1954 range in Fig. 5. Beneath this splendor of gleaming porcelain and chrome are 235 individual pieces that are joined together by 1150 spot and projection welds. And the amazing part of it is, these 1150 welds required just under one hour of productive labor as calculated from our time studies. And this time includes handling of all parts into and out of the welders as well. Think for a moment what time would be required if these parts were riveted, screwed or even fusion welded together. This is proof of the effect of this tool as used in high production.

Figure 5

Figure 6

RANGE WELDERS

But how are these 1150 welds produced in less than an hour? Let's look into some of the special high-production welders that help do this job. First, in Fig. 6, we see the basic structure of the range, the range frame, being welded together on this special 10-headed projection welder. A front and back panel, two top angles and three bottom angles are projection welded together in 10 locations simultaneously in less than minute. The frame is then completed in a gun welding buck where corner gussets are added.

The oven lining is another important assembly in the

range. Figure 7 shows the welder which takes a flat sheet and folds it up to form the bottom and sides of the oven, then seam welds the top panel in place with two series seam welds. The next welder assembles the front and back to the oven sides and tack welds them together with 20 spot welds, preparing it for seam welding. This is done in the dual head series seam welder shown in Fig. 8. The center fixture, holding the oven, rotates about its center, making two rolling spot seam welds all around the oven.

A typical range drawer welder, of which we have four different sizes, is shown in Fig. 9. Here the front, back and sides are welded together with 40 spot welds in one operation. The fixture slides in and out for loading, and hydraulic guns and package transformers are used for welding.

Figure 10 shows a rather interesting index welder we recently built up for welding three different assemblies. They are the right- and left-oven door hinge brackets and the hinge butt. They have a similar edge projection welded joint but they were formerly welded on two separate standard welders at a rate of 180 pieces per hour. When we first studied the job we soon found that our volume of production would never pay for two index fixtures, so we designed one fixture to take care of both. The fixture has 10 stations, the odd stations being for the hinge butt, the even stations for the right or left hinge bracket. The indexing gear has 10 teeth

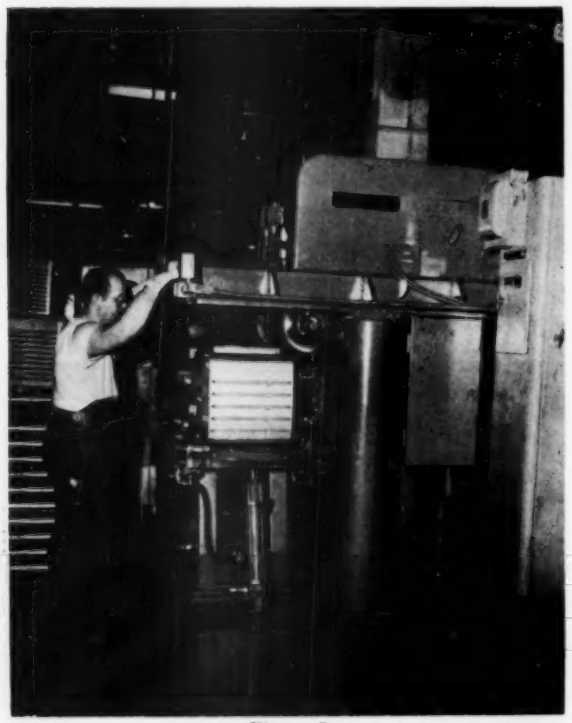


Figure 7

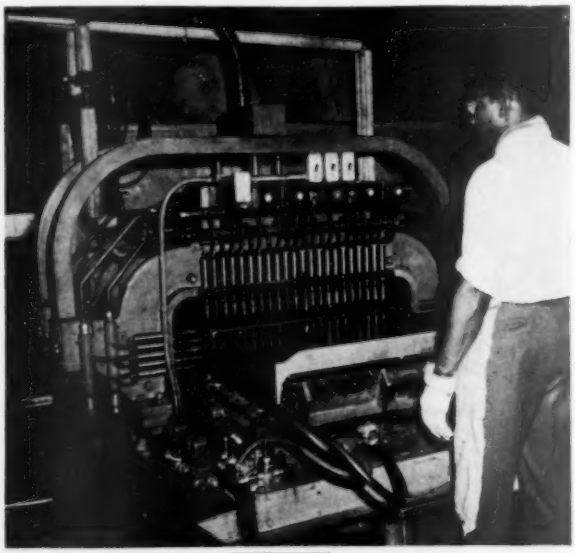


Figure 9

Figure 8

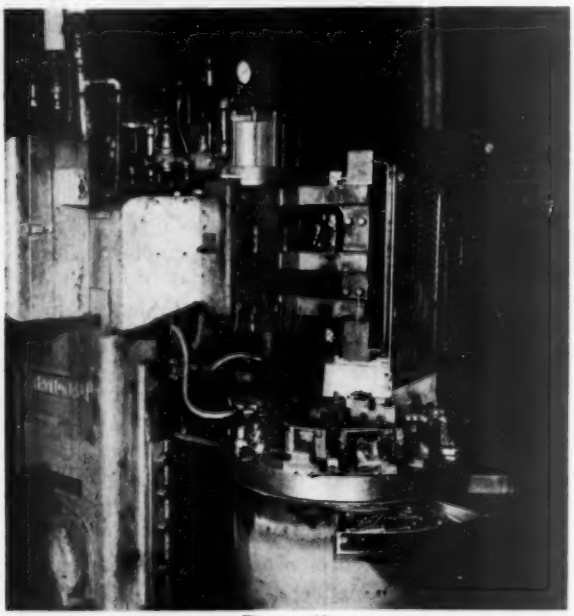


Figure 10

but the air-cylinder-operated ratchet engages only every other tooth, giving a double index. Thus, only five of the stations are used at any one time. Since a single upper electrode and unloader serve both jobs, the only job change-over consists of indexing the table one station by hand. The new production rate gives us a direct labor savings per year of nearly twice the cost of the tooling.

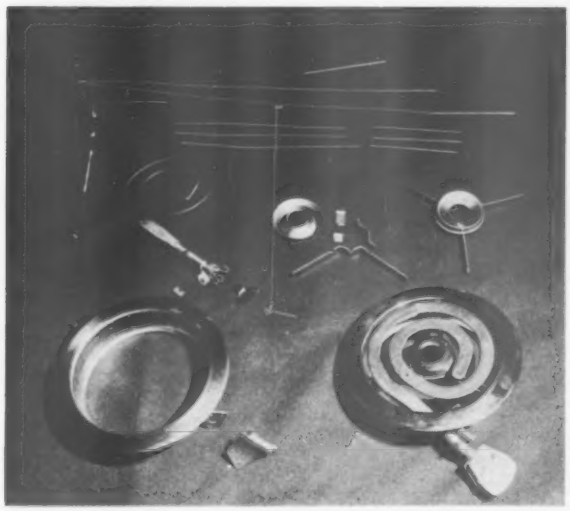


Figure 11

Figure 11 shows the many welded parts that go into the range surface heating units. Stainless steel terminals are welded to the nichrome wire; after the wires are strung in the tubes and filled with magnesium oxide, an Inconel cap is Heliarc welded to the Inconel tube. The tube is then coiled and swaged to shape, after which a stainless steel heater locator and ground clip are projection welded to the tube. A stainless steel mounting bracket is projection welded to the stainless steel adapter ring to complete the assembly. Figure 12 shows a 3-headed flat top welder for welding three terminal stubs to the terminal cluster which goes into the toe of the heating unit. This welder is unique in that

Figure 12

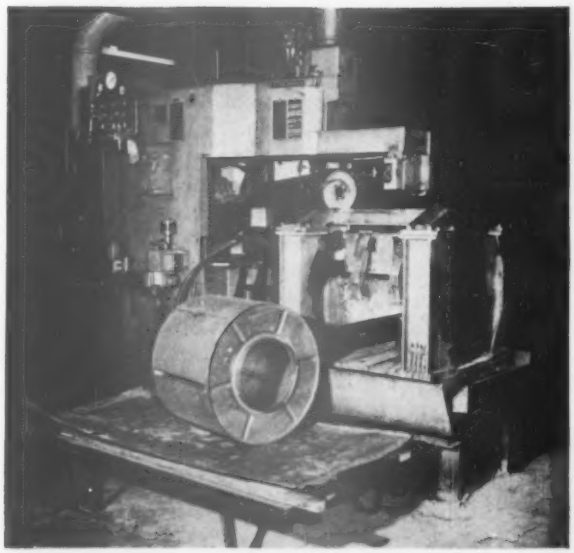


Figure 13

it has 3 single-phase transformers connected together 3-phase delta-primary Y-secondary to make all three welds with equal heat simultaneously. When this job was being set up, many ways were tried but it was found that this was the only way this job could be done satisfactorily. Notice that all three electrodes travel only a small distance into weld position, thereby serving as locators as well as electrodes.

DRYER WELDERS

The electric clothes dryer also has several interesting welding operations. The dryer drum longitudinal seam welder is shown in Fig. 13. Here the wrapper is taken in the flat and located over pins, giving the proper lap, and semimash seam welded on the traveling carriage seam welder, giving a smooth surface on the interior of

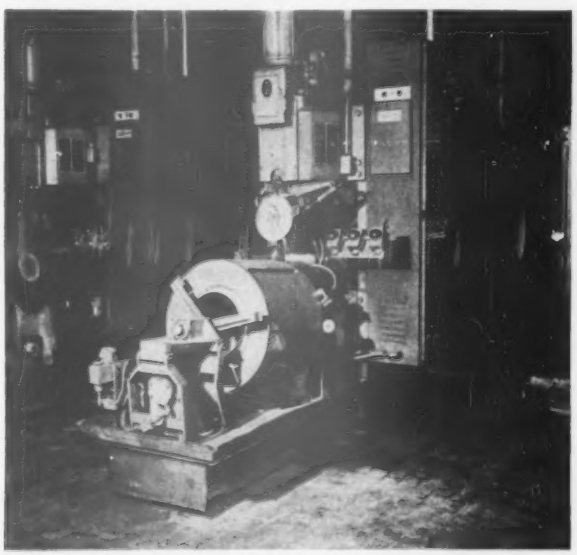


Figure 14

the drum. The drum heads are then assembled and tack welded in place for seam welding on the circumferential seam welder shown here in Fig. 14. The joint is a series of rolling spots spaced about 1 in. apart. The material is enameling iron. For clearance purposes, it was necessary to set these welding heads at a 6-deg angle. Because of this offset load on the seam welding bearings, special constant friction heads were employed. These heads use insulated tapered roller bearings to carry the mechanical load and silver contact brushes to carry the electrical load. They have proved so satisfactory in eliminating maintenance troubles that we are considering them on all future seam welding applications.

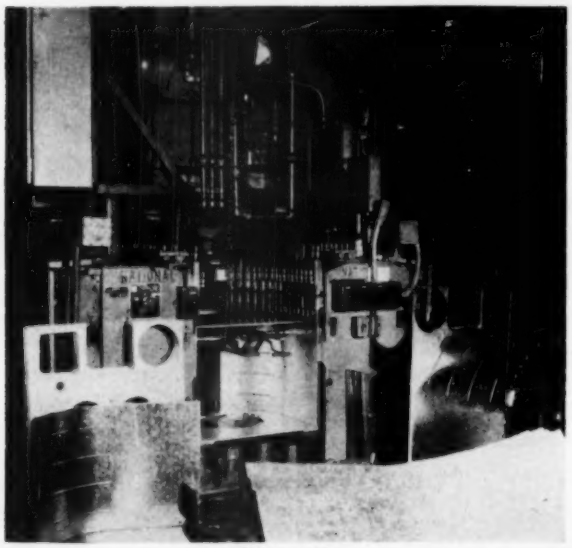


Figure 15

Figure 15 shows the multipoint welder which projection welds the front and back of the dryer frame to the sides. The material is 0.040 in. galvanized sheet steel which is normally rather difficult to weld. If spot welded, considerable trouble is experienced with electrode pickup and sticking. But by using individual hydraulic guns on each projection with a push-pull package transformer arrangement, successful and consistent welding has been possible.

Edge butt welding of a drain tube to a drain cup is shown in Fig. 16. A satisfactory design for this joint caused us a great deal of concern until we fell upon the idea of extruding the hole in the drain cup into which the tube was to be welded. It was then a simple job for this standard press welder.

AUTOMATIC WASHER WELDERS

Let's take a look at several interesting operations and assemblies on the automatic washer. One new assembly we have taken over since Plant 3 was started is the tub assembly. In Fig. 17 we see the first operation where the tub rim is pressed into the tub rim housing and spot tack welded prior to seam welding.

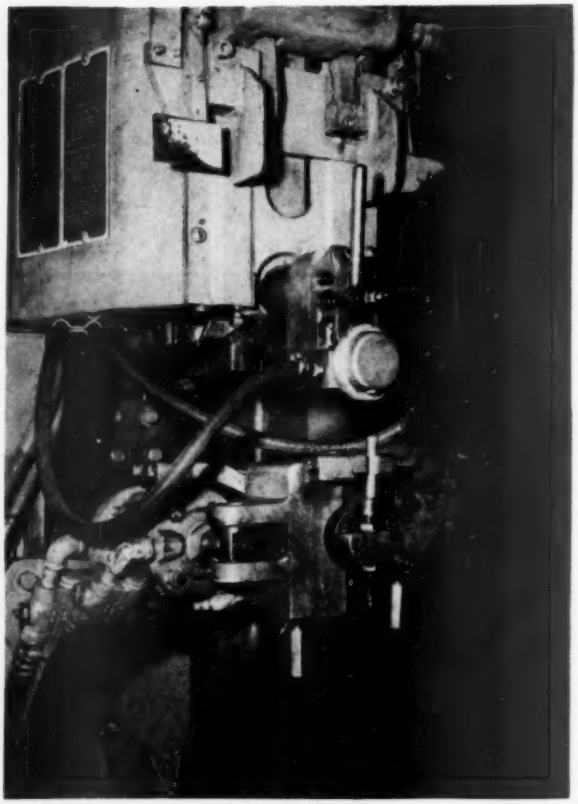


Figure 16

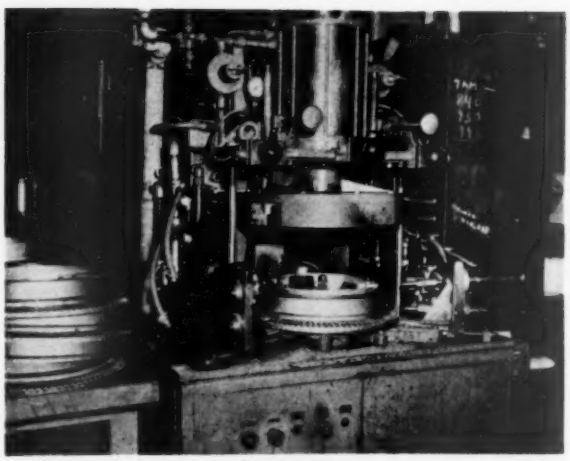


Figure 17

Note how the pressing and tacking are combined in one machine. The parts are then seam welded on this special dual head seam welder shown in Fig. 18. To make these two pressure-tight seam welds simultaneously, it is necessary to use separate pressure rams, separate welding transformers and separate synchronous controls for each weld. Figure 19 shows the welding of a reinforcing ring to the bottom of the tub on this multispot welder. The center fixture hinges out for loading and indexes to four positions to make a total of 16 welds. Next, six reinforcement plates are projection

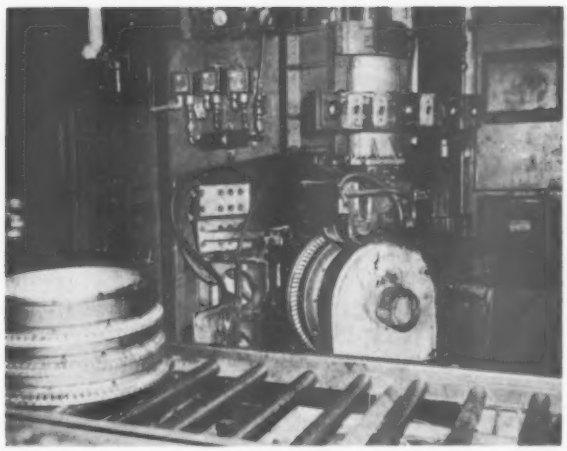


Figure 18

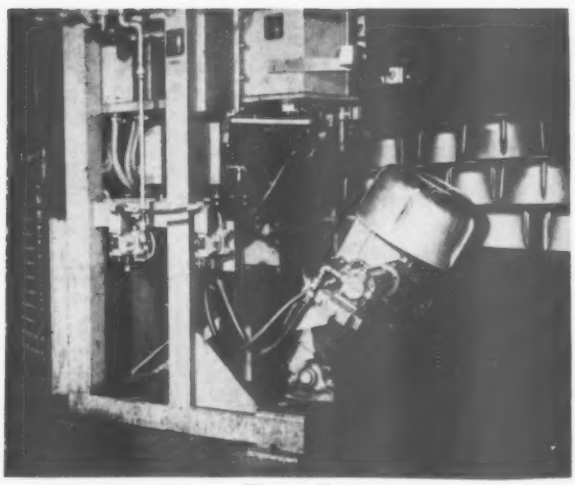


Figure 19

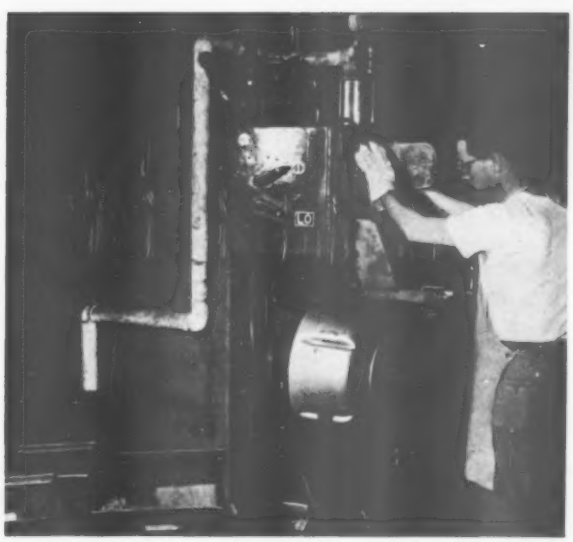


Figure 20

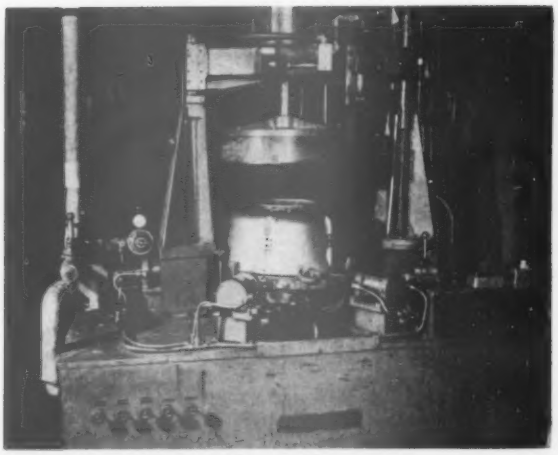


Figure 21

welded to the tub on the press welder with index fixture as shown in Fig. 20. A shuttle feeds the plates into the upper electrode one at a time, speeding up the loading time and keeping the operator's hands clear of the electrodes. The tub and the rim assembly are then pressed together in the machine shown in Fig. 21 and spot tack welded at the same time. The fit of the parts is so close that 5000 lb force is required to press them together. The tub assembly, which is entirely enameling iron, is completed by semimash seam welding this joint, ironing it out smooth on the inside surface on the circular seam welder shown in Fig. 22.

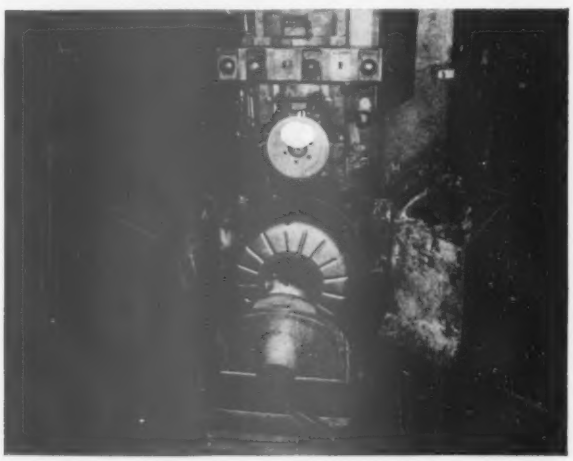


Figure 22

The cabinet shell of the automatic washer is also a welded assembly. In Fig. 23 we see the first welder in this line which folds up the sides of the shell from the flat and seam welds the back panel in place with two longitudinal seam welds made on this traveling carriage dual head series seam welder. Figure 24 shows a special 4-headed projection welder which welds the four bottom gussets to the shell. Note how the welder is built at conveyor height so that the shell can slide directly on the loading elevator which lowers the shell into welding position. The inner heads then expand

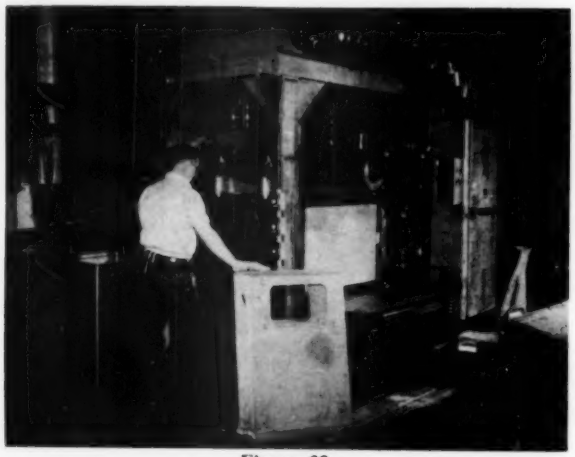


Figure 23

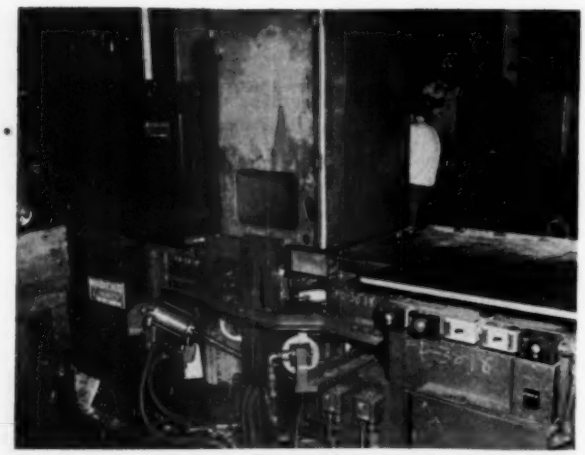


Figure 24

Figure 25

and the two transformers fire in sequence to make two series welds each. The elevator then returns the shell to conveyor height for unloading without any lifting of the shell.

In Fig. 25 we see another 4-headed projection welder for welding the top gussets to the shell. This welder is similar to the previous welder except that it is built upside down. This was done so the gussets could be loaded on the lower electrodes since they are welded on the inside of the shell. Also, it eliminated turning the shell upside down for this operation and back again for the next operation.

DESIGN CONSIDERATIONS

In our discussion of welding equipment for high production, we must give a great deal of consideration to design, both joint design and equipment design. We are all quite familiar with the more standard time-proved design rules, so the following cases illustrate a few that may be out of the ordinary that we have come across.

First of all, try to incorporate some method of self location into your joint design. This can be done in many ways, but one simple way is shown in Fig. 26 where we see a cup and nut being projection welded to a plate. In the "Wrong" joint design, a pin locator is used through all three pieces, causing a constant burning of the pin and the threads. In the "Right" joint design, the nut includes a pilot which locates the other pieces and protects the threads.

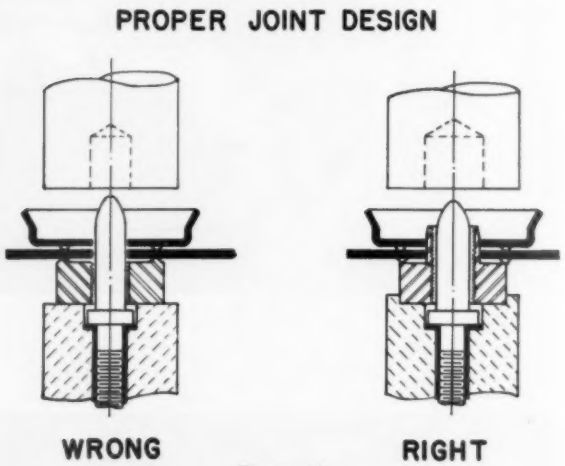


Figure 26

Series welding is a wonderful way to get double use out of welding current, but there are certain rules which must be followed. The most important one is to force the welding current through the joints instead of allowing it to shunt across one sheet. This is illustrated in Fig. 27 where two brackets are being welded to a panel. The "Wrong" method shows the current being introduced on the panel side where it may shunt across. The "Right" method is to introduce the current from the bracket side, so the current must go through the projections in order to complete its circuit. In this case it had an added advantage because the top side of the panel was a "show" side, later receiving a white porcelain finish. Surface burning on this side is greatly reduced because the sheet again carries a majority of the current, but this time to our advantage.

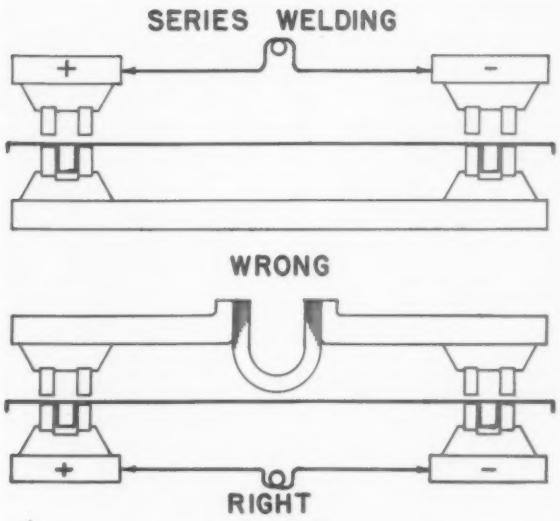


Figure 27

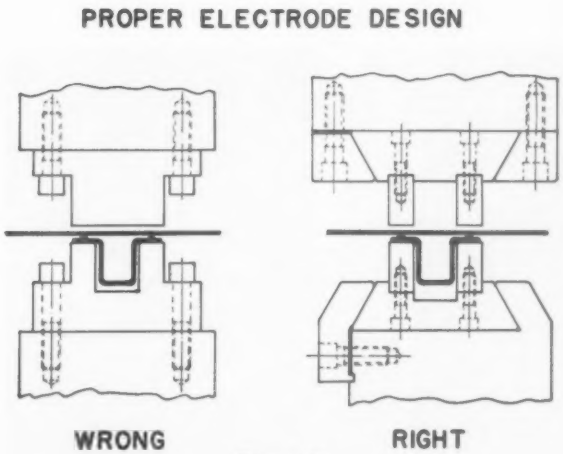


Figure 28

In the design of resistance-welding equipment, the important part of the tooling is properly designed electrode inserts, not only to give good welding results but also to reduce maintenance on these highly perishable items. Figure 28 shows several ways in which electrodes can be designed to help the maintenance man. First of all, small inserts are used instead of one large block because they are easier to make and they conserve copper alloy. Next a standard gib design allows the electrodes to be removed by merely loosening the gib screws rather than removing several screws entirely. The screw heads should also be protected from welding flash, if possible.

If there is an angle involved in the parts being welded, try to design the electrodes so that the alignment of the parts will not be disturbed when the electrodes are dressed after wear. This is shown in Fig. 29. If the "Wrong" electrode is used, the lower el. trode must be discarded after one dressing. But if the "Right" electrode is used, the electrode may be dressed often and can easily be adjusted for wear with the jack screw.

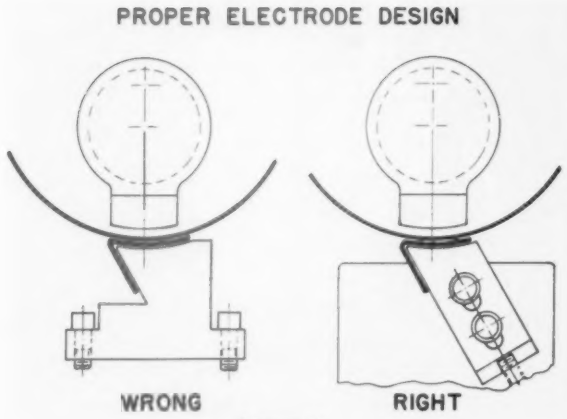


Figure 29

Figure 30 illustrates what we have called a movable electrode design. Essentially it is an electrode insert which can be adjusted back and forth to distribute wear along a greater surface of the electrode. The left section shows it being used on spot welding. On one of our applications where we were welding aluminum clad steel, we found the lower electrode life to be several dozen times longer after we made it movable because of the high pickup of aluminum. The projection welding application showed a similar savings. In this case, the show side of the panel was against the movable electrode, thereby reducing the metal finish required on the panel as well as reducing the maintenance problem.

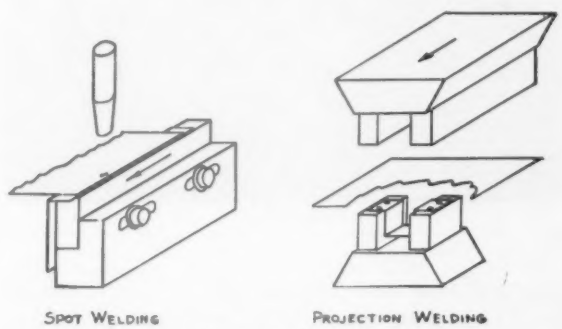


Fig. 30 Movable electrode design

SAFETY CONSIDERATIONS

At Frigidaire we are quite proud of our safety record and the emphasis that we place on safety considerations. General Motors and Frigidaire spend great sums of money each year for safety devices of all types. Of course, the primary concern of our department is to make welders more safe. Several years ago we began a program of installing mechanical safety latches of the type shown in Fig. 31 on all our press welders. This program is now complete, and all new welders require these latches upon installation. These dual latches keep the ram from operating for any reason until the operator presses both palm buttons to swing the latches

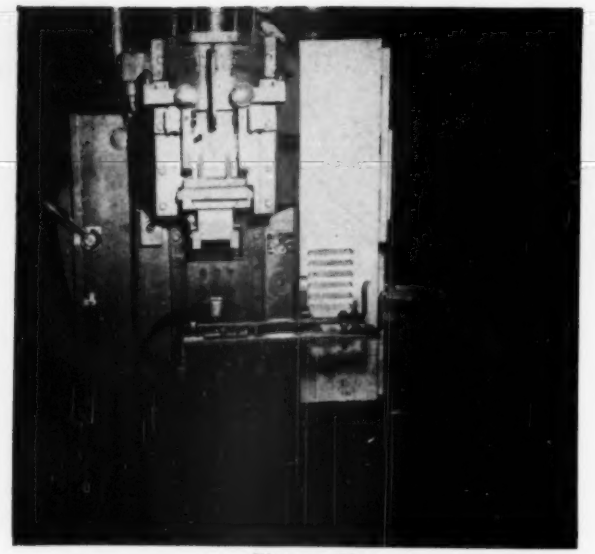


Figure 31

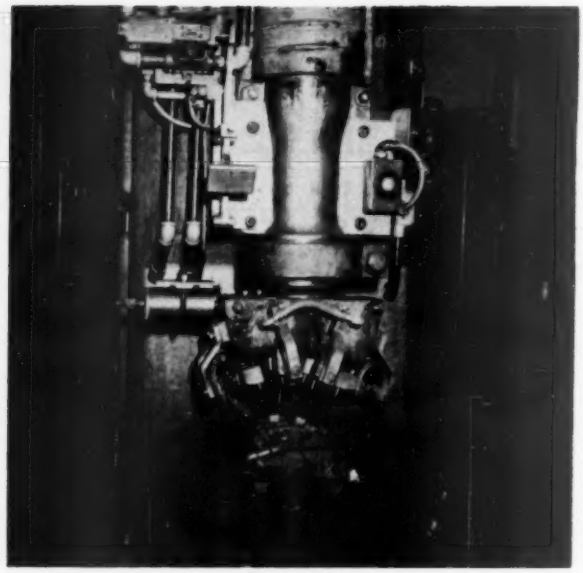


Figure 33

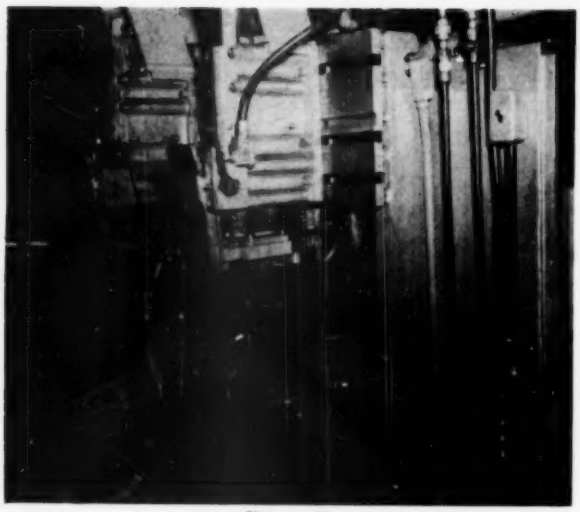


Figure 32

Figure 34

clear and close two limit switches. They return by gravity, so they are virtually foolproof. This particular set of latches, since it is on a rather long stroke welder, is so connected that the operator must keep his hands on the buttons until the weld begins. Figure 32 shows a dual-head press welder with one latch on each head. Notice also that hand clamps are used on this fixture instead of air operated ones because they would be much more dangerous.

In Fig. 33 we see a welder for which we found it virtually impossible to design a set of mechanical safety latches. This was because the large upper electrode fixture is mounted on a rather small diameter shaft. The off-center load which would occur if only one latch would catch would be enough to distort the whole fixture. In this case, we have devised a blocked off air valve to serve as a safety device. Note that the solenoid air valve mounted on the upper left of the welder has a lever bar along side of it. The valve is of the sliding spool type, the shaft of the spool projecting out from the valve body about one-quarter of an inch when the solenoid is energized. The lever bar prevents the valve from operating for any reason until the operator pushes the lever back with one hand, closing a limit switch, and pushes a palm button with the other hand. We have also used these blocked off valves on multi-head welders where the application of safety latches was impractical.

Figure 34 shows a special welder for welding four bulkhead support strips to the washer shell. It is essentially an inverted press welder since the ram is mounted on the lower portion and the knee mounted above. Here again, conventional safety latches were impractical, so in this case we have air-operated latches. However, to give added safety, the circuit is so

connected that the one palm button pushed by the operator unlocks both latches which in turn hit limit switches in series with the other palm button. Thus, more than one switch or solenoid valve coil would have to short out to cause an accident. Note also the choke coil mounted on top of this welder to ground the secondary of the transformer. In most conventional welders one side of the secondary is grounded directly to the frame of the welder to prevent the operator from forming a ground if the transformer shorts out primary to secondary. This direct ground is not always practical, as in this case where it was impractical to insulate the entire lower fixture. Therefore the fixture was bolted directly to the frame and both sides of the secondary were insulated. The choke coil is connected secondary to ground, allowing a very low amperage to leak to ground at normal welding voltages. But if 440 v should develop in the secondary, the coil would form a direct ground.

CONCLUSION

By giving these few examples of specialized welding equipment, we hope that we have shown how high production welding is applied in the appliance industry. But ours is not always a problem of higher production rates from the machines themselves. In all these cases shown, material handling was just as important a problem, and more complete automation is coming into the picture very rapidly. Proper consideration must be given to both the design of welded joints and the design of equipment if satisfactory results are to be obtained.

Once a welder is in production, it must be kept running. Therefore the machine must be built with the maintenance man in mind. And most important of all, it must be safe, safe for every one who works around it.

Resistance welding has done much to supply the needs of a growing market in the appliance industry at a price which is attractive to this market. But we should never forget that without the great volume demand which we try to satisfy, all of our high-priced special equipment would be impossible. Supplying more goods at lower costs so that more people might enjoy them is the part of our economy which has made our country great. May our technical advances help it to continue in this direction.

Current Welding Literature

(*Continued from page 758*)

subsurface cracks formed at fusion lines between austenitic weld metal and ferritic pipe; implications of findings.

Welded Steel Structures. Problems of Fabrication and Erection in All-Welded Multi-Story Framed Buildings, D. C. C. Dixon. Engineer, vol. 196, no. 5106 (Dec. 4, 1953), pp. 749–751. Because members of present-day multistory building can be divided into classifications, possibilities of line production methods are indicated; main field for development is in handling equipment (mainly manipulators). From paper before Conference on Welded Structures, Instn. Civ. Engrs.

Welding, Electric Arc. Self Adjusting Arc and Controlled Arc Welding Processes. W. G. Hull and J. C. Needham. Brit Elec. & Allied Industries Research Assn.—Tech. Report Z/193 (1953), 31 pp., 7*s*. Two new arc-welding processes, both of which employ consumable electrode and gaseous shielding of arc, developed in America; with former, welding can be carried out manually in all positions in plate thicknesses from about $^1/_8$ in. to maximum thickness available, while latter is used for mechanized welding of plate of $^3/_8$ in. thickness and over; published information up to December 1952 summarized.

Welding, Electric Arc. Self-Adjusting Welding Arcs, J. C. Needham. Brit. Elec. & Allied Industries Research Assn.—Tech. Report Z/T91 (1953), 19 pp., 10*s*6*d*. Report deals with preliminary examination of mechanism of arc length control of "self-adjusting" arc and particularly with effects of output characteristics of welding power source on degree of self adjustment of arc length.

Welding, Gas. Atomic Hydrogen in Research Laboratory, R. L. F. Boyd and N. D. Twiddy. Electronic Eng., vol. 26, no. 321 (Feb. 1954), pp. 78–79. Instrument for welding or fusing metals using *r-f* corona to produce dissociation of hydrogen jet, requiring no electrode emission and utilizing cold electrodes; applications for small parts such as thermocouple wires and dentures.

The Effect of Welding Variables on Hard-Facing Deposits

Hard-facing deposits are affected by the welding variables which determine deposit analysis and cooling rate of the deposit

by A. Zvanut and V. Peters

INTRODUCTION

Alloy steels are special-purpose steels for high-tensile strength and other special requirements. They differ from mild steel in that they have higher carbon and alloy content. The higher carbon and alloy content confer the special properties to these steels. The special properties, however, are only available for use through proper heat treatment of the steel. Heat treatment determines the structure of the steel which, in turn, determines properties.

Hard-facing with the electric arc essentially duplicates the electric furnace process of making alloy steels. Hard facing welding electrodes contain carbon and other alloying materials which are introduced into the "furnace" of the welding arc so as to produce deposits of alloy steels and irons. Simultaneously, the heat cycle of the welding process produces a heat treatment. Hard facing is thus a process for simultaneously alloying and heat treating.

In hard facing with the electric arc, as with all steel making, the process of alloying and heat treating must be precisely controlled to produce satisfactory structures for abrasion and impact resistance. The electrode manufacturer must correctly balance his electrode formulation to provide for variables that enter the process, and the consumer must control the welding procedures to produce a satisfactory deposit. The rapid heat input and fast cooling of the weld deposit can materially alter the structure, and, therefore, the properties of the deposit.

The control of welding variables in hard facing determines the results achieved. Frequently, insufficient attention is given to the control of these variables, and it is the purpose of this paper to indicate what variables are significant and in general how they should be controlled. The discussion will not be a technical one, and the engineer perhaps will be surprised at the absence of photomicrographs and a discussion of metallurgy which usually accompanies a paper of this type. The authors hope to reach the man in the field with practical information on the control of variables to help him obtain the best possible hard-facing deposits at the most economical cost.

For simplicity the discussion will be confined to two types of electrodes—chromium carbide and semiaustenitic. These are the most widely used types of hard-facing electrodes.

CHROMIUM CARBIDE TYPE

Chromium-carbide type electrodes are available in either tube or cast form. The undiluted deposits contain approximately 5% carbon, 25% chromium and other alloying elements. The complex carbide particles in the deposit give it very high abrasion resistance, and the alloyed iron matrix gives it a certain degree of toughness. The deposit is nonmachinable and must be finished by grinding. This type of electrode is used where abrasion is severe with moderate impact.

Welding variables determine the structure of the deposit, and, therefore, its properties. These variables will affect single-layer deposits, but will have little effect on deposits of two or more layers.

SEMIAUSTENITIC TYPE

The semiaustenitic type is a coated, shielded-arc electrode. The undiluted deposits contain approximately 1–2% carbon, 5–12% chromium and other alloying elements. The total alloy content is less than 20%. The deposit is used to resist severe impact with moderately severe abrasion. It is a work-hardening deposit. Pounding and battering develops additional hardness on the surface of the deposit. It is nonmachinable and must be finished by grinding. It can be hot forged. This type has more uses than any other type and is priced only slightly higher than mild steel electrodes.

A. Zvanut and **V. Peters** are Development Engineers, Lincoln Electric Co., Cleveland, Ohio.

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Welding variables will produce a wide range of structures in these types of deposits, and it is important, therefore, in using them to give important consideration to the manufacturer's recommendations for correct procedure.

A study of these variables and their control present an inevitable difficulty in that it is impossible to obtain data for one variable alone without the other's influencing test results. Test data presented here must be accepted and applied with this reservation in mind. In every field application the effect of variables must be correlated.

Abrasion tests for this paper were made on an anchored-type abrasion tester. The test employs a 1-in. wide, 8-in. diam. alundum grinding wheel revolving at 30 rpm abrading a 0.5-in. round specimen under a load of 50 lb. The abrasion values reported are versus an SAE 1020 steel standard. The standard has a value of 32. A slow rate of wear as may be obtained from specimens cut out from the weld is indicated by small values. For example, a properly applied semiaustenitic deposit will have a value of 3 to 4.

Hardness values reported were determined by the Rockwell method using a diamond indentor under a 150-kg load (C scale).

VARIABLES

The properties and the performance of a hard-facing deposit depends on the structure of the deposit. The most important factors determining this structure are the variables:

1. Deposit analysis
2. Cooling rate

The deposit analysis will determine what structures it is possible to form, and the cooling rate will determine what structures are actually formed.

Deposit Analysis

The deposit analysis is determined by the *electrode design* and how the electrode is used to control the extent of *dilution* of electrode metal by the base metal. The manufacturer of the electrode supplies certain alloys through his electrode, and in its design controls the arc characteristics, in order to minimize base metal dilution. The consumer, however, controls the final results through his welding procedures. The consumer may start out with an electrode of high-alloy content but, through improper procedures, end up with a deposit of less abrasion resistance than a low-alloy electrode.

Tests have indicated that as the alloy content of the deposit is increased, abrasion resistance will increase. Table 1 presents data to show the relative abrasion resistance of single-layer deposits made with electrodes of varying alloy content. The single-layer deposits were all made with a constant current on the edge of a medium carbon plate $^3/_8$ x 4 x 18 in. with a $1^1/_4$-in. weave. Three different semiaustenitic electrodes were used whose analyses were determined from undiluted weld deposits. Specimens cut out from the single-layer deposit of the electrode with the highest alloy content showed an abrasion value of 3.0 while the lowest alloy content produced a value of 9.3.

The deposit analysis in this case was controlled for all the specimens by the use of identical procedures in order to show the effect of electrodes of varying alloy content. The variation in performance was due to variation in structure as the different alloys responded differently to the same cooling rate. If one of the variables controlling cooling rate had been changed, such as current or mass of base metal, the results would have been different. It cannot, therefore, be assumed that simply because an electrode contains a large amount of alloy its use will give maximum abrasion resistance. Variables may alter the deposit structure sufficiently so that abrasion resistance will be lower than that normally expected from a high alloy deposit.

Such was the case of a manufacturer of scraper blades who was surfacing the cutting edge with a chromium-carbide type electrode. Procedure variables of dilution and cooling rate were affecting abrasion resistance, and through tests it was discovered that better results could be obtained with a lower priced, lower alloy semiaustenitic deposit. The manufacturer has switched and is now depositing at less cost the lower alloy material that has proved superior to the higher priced, higher alloy deposit.

Base Metal Dilution

The alloy content of the hard-facing deposit is the admixture of base metal and the electrode. Electrodes are designed to compensate to some extent for base metal dilution, but procedures can create a wide variation in dilution and, consequently, in results obtained.

Single layer applications are susceptible to the greatest amount of dilution and, therefore, variation in deposit analysis. The effect is progressively diminished in multiple layer deposits since the deposit analysis tends to become stabilized. Two layer depos-

Table 1—Effect of Alloy Content of Single-Layer Deposit

*Electrode**	*C*	*Cr*	*Mn*	*Si*	*Mo*	*V*	*HR$_c$*	*Abrasion value*	*Thickness of deposits*
A (semiaustenitic)	1.3	3.6	...	0.3	...	..	50	9.3	0.123
B (semiaustentic)	2.0	7.0	1.3	0.9	0.4	..	51	3.1	0.126
C (semiaustentic)	2.5	12.2	2.0	2.0	...	..	39	3.0	0.126

* Analyses given are for nondiluted deposit.

Table 2—Effect of Base Metal Dilution on Hard-Facing Deposits

*Electrode**	*C*	*Cr*	*Mn*	*Mo*	*Si*	*V*	*No. of layers*	*Thickness of deposits*	*HR_c*	*Abrasion value*
A (semiaustenitic)	1.3	5.0	0.51	...	0.88		1	0.155	59	10.0
							2	0.228	56	7.0
							6	0.433	28.5	3.2
B (chromium carbide)	4	26	5.5	0.1	0.3	0.05	1	0.083	53	5.0
							2	0.175	59	0.87
							4	0.310	51	0.43

* Analyses given are for nondiluted deposit.

its are, therefore, commonly recommended for applications for resisting abrasion. On these, procedure variations will have less effect. If a one layer deposit is used additional precautions to minimize dilution are necessary. The following variables should be controlled to minimize dilution:

1. Welding speeds—use slower speeds.
2. Welding currents—use lower currents consistent with proper fusion with base metal.
3. Arc action—use welding technique to direct the arc on the molten metal.

For their effect on base metal dilution the following additional procedure variables should also be controlled, when possible, in the direction of less penetration:

4. Bead size
5. Bead shape.
6. Bead overlap.
7. Electrode size.
8. Polarity.
9. Voltage.
10. Position of welding.

Table 2 presents abrasion values of specimens cut out of deposits of varying thickness. The increase in abrasion value as the number of layers increases indicates the effect of the larger amounts of alloy in the multiple-layer deposits which are less diluted by base metal. The analyses given in the table are electrode analyses determined from nondiluted deposits. No analyses are given for the actual test specimens.

Although the data show high abrasion values for the relatively thick deposits, in practice thick overlays of hard-facing materials are seldom required. These tests indicate the significance of base metal dilution for hard-facing performance, but in practice it can usually be controlled through variations in procedure. It is seldom necessary to obtain nondiluted deposits through making a large number of layers. It is customary to build up the part to within one or two layers of desired size with a high-strength electrode and then add the final layer or two of protection material, controlling dilution by controlling procedure variables.

Cooling Rate

The electrode analysis and the amount of base metal dilution determine the deposit analysis. The structure of this deposit determines its performance. The structure can be changed significantly by the heat treatment of the welding cycle. The amount of change will vary from practically nothing to almost a complete change depending on whether or not the electrode used deposits an alloy whose structure depends on the cooling rate. Some deposits, provided they are not diluted by the base metal, are practically unaffected by cooling rate. The chromium-carbide type electrodes are an example. Alloy content of the electrode is the determining factor. The chief function of alloys in welding electrode deposits, as in steel, is the control of structure. Carbon in welding electrode deposits, as in steel, confers hardness.

The heat treatment of the welding cycle is controlled by increasing or decreasing the cooling rate of the weld deposit. If the electrode being used deposits an alloy that is affected by cooling rate, the following factors are controlling:

1. Heat input—determined by: ambient temperature.
   Preheat.
   Interpass temperature.
   Welding speed.
   Bead size and shape.
   Electrode size.
   Polarity.
   Welding current.
2. Heat dissipation—determined by the ability of the mass (section dimensions and thickness) to draw heat away from the deposited metal.

The summation of the effects of heat input and mass control the cooling rate and, therefore, the heat treatment of the deposited metal. In general, if the alloy deposited is one that is changed by heat treatment, as the alloy content decreases the cooling rate should be increased to give best abrasion resistance. As the alloy content increases, the cooling rate can be slower.

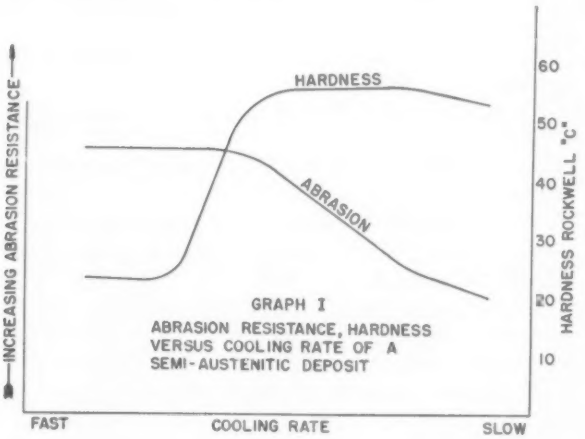
The effect of cooling rate on structure is most commonly measured by hardness, and this is taken as a basis for predicting wear. Hardness, however, can be quite misleading and should not be relied upon unless sufficient evidence is presented to warrant its use. Most consumers do not have adequate testing facilities to secure the necessary information to be able to use hardness as a wear resistance index.

If the alloys in the deposit are ample and susceptible to heat treatment, a fast cooling rate will produce low hardness readings which indicate a large portion of aus-

tenite forms the structure. Austenite is nonmagnetic, soft and tough, but has good abrasion resistance under most conditions. Its surface hardness, however, increases rapidly by cold working, yet its abrasion resistance remains materially the same.

With a slower cooling rate, higher hardness readings indicate that a substantial amount of austenite, or possibly all of the austenite, has transformed to martensite. Martensite is magnetic; has both high hardness and good abrasion resistance. Its ductility is low but is adequate for most hard-facing requirements.

If the cooling rate is further retarded, mixed structures of bainite, cementite and some martensite may appear. They may show high hardness readings, but the abrasion resistance is poor.

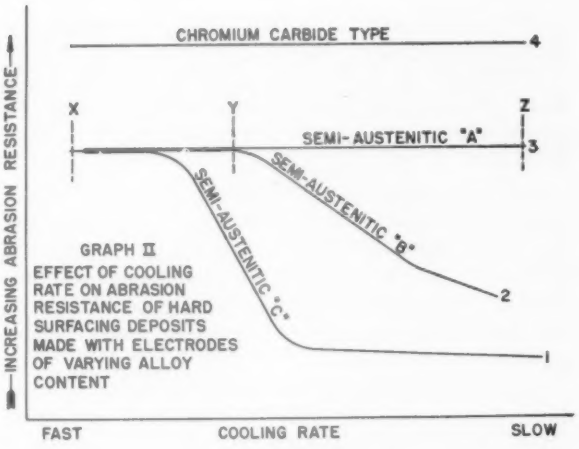


Graph I

Graph I plots the curve of the abrasion resistance values of a semiaustenitic deposit subjected to varying cooling rates against the curve of the hardness readings. The best abrasion resistance is obtained with the lowest hardness, and the abrasion resistance decreases as hardness increases.

The extent to which cooling rate can change structure and, therefore, the wearing performance of hard facings is indicated by the curves on Graph II. The left-hand portion of the curve represents a cooling rate obtained by using the manufacturer's recommended current for the electrode and applying the second layer when the temperature of the first layer cooled to 200° F. Subsequent points were obtained by depositing the second layer at interpass temperatures of 500, 750 and 1055° F, respectively, to slow down the cooling rate of the weld deposit. The weave bead was deposited on the 1-in. edge of a $1^1/_2$- x 1- x 6-plate using constant current of 200 amp.

Curve No. 1 represents an electrode which deposits an alloy of $1^1/_2$% carbon and 5% chromium and other alloying elements. (All analyses given are for nondiluted deposits.) Observe the decrease in abrasion resistance as the cooling rate is retarded. The structures developed are high temperature or "intermediate" products which are detrimental to abrasion resistance.



Graph II

These deposits will show higher hardness readings than those subjected to a fast cooling rate, but poorer abrasion resistant qualities.

Curve No. 2 indicates a deposit of higher alloy composition. The increase in alloy content (2% C – 7% Cr) allows the abrasion resistance to remain constant even though the cooling rate has been slowed down considerably. When the abrasion resistance does start to drop, the decrease is not as appreciable as the deposit in Curve No. 1.

Curve No. 3 represents a deposit of $2^1/_2$% C and 12% Cr. Curve No. 4 represents a deposit of 5% C and 25% Cr. The abrasion resistance of both these deposits is practically unaffected by the cooling rate of the weld deposit. The consumer has a distinct advantage in the use of a higher alloy electrode—any method of deposition for two or more layers will result in good wearing qualities. An electrode of this type, however, usually implies a sacrifice of some impact resistance, and the increase in price, due to higher alloy content, deserves consideration.

Further information on the importance of cooling rate is revealed from a reading of Graph II at points *x*, *y* and *z*. At point *x*, all three electrodes, A, B and C, with a fast cooling rate, have the same abrasion resistance. At point y, a slower cooling rate, electrodes A and B have equal abrasion resistance. At point z, the slowest cooling rate, only electrode A would be satisfactory for abrasion resistance. Electrodes B and C will cost less than electrode A because of the lower alloy content.

The control of cooling rate is essential to getting the best possible results at the lowest possible cost. Its control, however, is sometimes a complex procedure since the factors that control heat input also affect base metal dilution, and the two must be properly correlated to produce the desired results. For example, while a slow welding speed may reduce base metal dilution, it can also increase heat input which may not be desired.

Or again, it may be necessary to preheat or keep the

deposit hot by welding at high interpass temperatures to eliminate cracking tendency resulting from stresses developed by expansion and contraction. This slows down the cooling rate which as we have seen results in poorer abrasion resistance, especially with low-alloy electrodes.

Still other service conditions may suggest using higher currents to increase penetration and weld metal dilution. The slower cooling rate with higher currents will increase resistance to impact, but will also add sufficient alloy to the base metal to impart an increase in abrasion resistance without danger of spalling under impact service.

Table 3 shows the effect of welding current on single layer hard-facing deposits. Because they are single layer, base metal dilution in this instance is increased by higher currents. It is also true that cooling rate is slowed down by the higher currents due to the greater heat input. As higher currents increase base metal dilution, abrasion resistance is decreased. Low currents decrease dilution and also increase cooling rate, both of which improve abrasion resistance. These tests were made on $^3/_8$- x 4- x 18-in. plate. If, instead, they had been made on a 1-in. plate increasing the mass and thus cooling rate, still better abrasion resistance could have been made. On thicker plate a current of 200 amp would have to be used. The 3.2 abrasion value recorded by electrode A at 150 amp would be obtained at 200 amp on the 1-in. plate.

Table 3—Effect of Current on Single-Layer Hard-Facing Deposits

Electrode	*Current amp*	HR_c	*Abrasion value*
A (semiaustenitic)	150	57	3.2
	200	54	9.4
B (semiaustenitic)	150	53	6.7
	200	59	10.0
C (chromium carbide)	110	49.5	3.6
	150	53	5.0
D (chromium carbide)	110	59	0.70
	150	59	0.90

It should also be noted that the abrasion value of the single-layer deposit of electrode A made at 150 amp is the same as that recorded by the six-layer deposit of this electrode made at 200 amp (Table 1). It is possible, therefore, that a single-layer deposit of an electrode will outwear a thick deposit of three or more layers of the same electrode. The cooling rate can completely change the structure of the deposit and its performance.

These typical examples of different combinations of variables indicate that cooling rate control must be properly correlated with the factors determining deposit analysis. The effects of the various factors involved are interrelated and seldom, if ever, isolated.

CONCLUSIONS

Hard-facing with the electric arc is a process for simultaneously alloying and heat treating. All hard-facing deposits are affected to some degree by the welding variables which determine deposit analysis (alloying) and cooling rate (heat treating) of the deposit. This discussion has not attempted to establish procedures for the control of variables because of the difficulty of establishing procedures that will be proper under the almost limitless variety and complexity of service conditions. Rather, through the analysis of laboratory test data, this paper has attempted to indicate areas and directions controls should take. For this, the following general conclusions can be made:

1. Hardness is not necessarily a measure of wear resistance.
2. As alloy content increases, abrasion resistance increases.
3. As base metal dilution increases, abrasion resistance decreases.
4. As alloy content increases, cooling rate can be decreased.
5. The results intended by the manufacturer for a given type electrode will not be obtained unless proper procedures are used.
6. Semiaustenitic, low-alloy electrodes are used to resist both abrasion and impact. Their deposits are affected by cooling rate. For abrasion values of 3–4 use two-layer deposits. Preheat, higher currents and high interpass temperatures improve impact resistance, but decrease abrasion resistance. For thin sections that provide slow cooling rates, use low currents. For heavy sections that provide a fast cooling rate, use higher currents.
7. Chromium-carbide type electrodes resist severe abrasive wear. Their deposits of two or more layers are not materially affected by cooling rate.
8. By correlating the control of factors that determine deposit analysis and cooling rate a wide variety of service requirements can be met.

The above general conclusions will necessarily be modified by the combination of circumstances and wear conditions encountered by the field. Conditions in the field seldom, if ever, duplicate those under which the test was obtained. Furthermore, as was stated, the test data must be qualified by the fact that so many of the variables are interrelated. It is impossible to isolate any one of them. It is important, therefore, that the applicator of hard-facing materials analyze the service involved before selecting an electrode and developing a procedure. Service conditions will indicate in which direction variables should be controlled.

Heliarc and Sigma Welding Team Up

THE Aluminum Goods Manufacturing Co. of Wisconsin, makers of Mirro aluminum products, has combined two modern fabricating methods—Heliarc and sigma welding—to overcome problems of expanding production. This team-up helped the company to meet defense needs for cartridge tanks, without seriously disrupting the plant's production of civilian goods. Heliarc welding speeds up to 60 ipm, and sigma welding speeds up to 200 ipm have been made possible with automatic setups. To date over one million cartridge tanks have been produced.

Cartridge tanks are made from 34-in. wide aluminum rolls. Blanks are stamped out of the aluminum, cleaned and formed into open cylinders about $4^1/_2$ in. in diameter. Formed blanks are fed into a modified tube welding machine equipped with a sigma welding torch.

After the ends of the welded cylinders are trimmed, a cup is welded to one end of the tube. This cup is made of 10-gage aluminum and is held in place by a backing mandrel inserted in the cylinder. The unit is inserted in a jig which rotates under a Heliarc torch. Efficient designing of the welding machine includes an automatic control which stops the machine after a weld is finished. Each weld is completed in an average time of $12^1/_2$ sec.

A grooved collar is pressed on the open end of the aluminum tube, and the cartridge is complete.

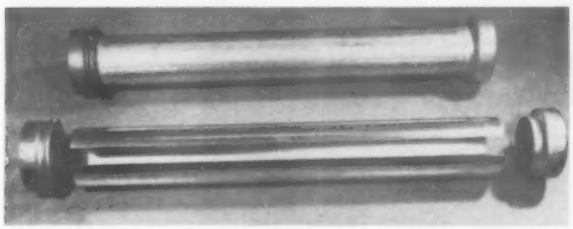


Fig. 1 Three aluminum parts (below)—a collar, cylinder and cup—when welded (above) form a cartridge tank

Fig. 2 Welded cylinders roll out of the tube welding machine. During welding process, cylinders move under the sigma torch within this machine at a speed up to 200 ipm

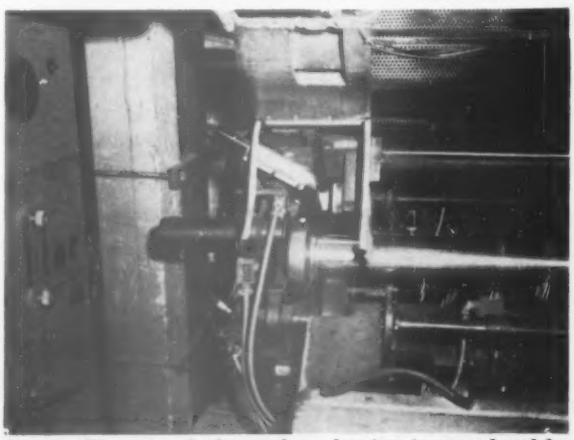
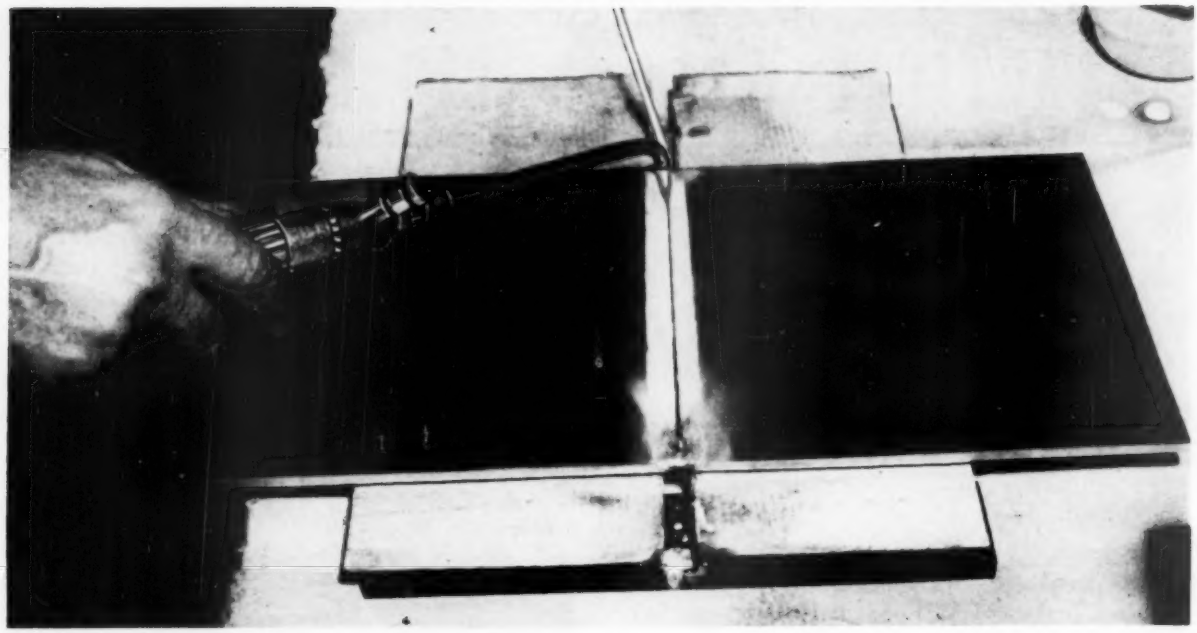


Fig. 3 The second phase of production is completed by this automatic Heliarc welding machine. The cup (under torch center) will be welded to the cylinder in $12^1/_2$ sec

These fabricating methods have proved successful on this job, and many of the same features are being applied to civilian production.



Welding Aluminum Sheet

This article has been prepared in answer to numerous requests. It is designed to help those welding operators who have infrequent occasion to weld aluminum by the oxy-acetylene process

By F. C. Geibig

Hints for the Beginner; Reminders for the Veteran

OXY-ACETYLENE welding of sheet aluminum can be difficult for the beginner and sometimes for the experienced operator because it is not easy to see the reaction of aluminum to heat. Aluminum oxide, which forms on the metal, also presents a problem in obtaining good fusion.

Aluminum has a low melting point, becomes weak when heated, does not change color when it begins to melt and conducts heat rapidly. Since heat is conducted rapidly in aluminum, you will need a fairly large welding flame to raise the metal to melting temperature. In most cases the same flame size that you use for welding steel of the same thickness is satisfactory. Placing asbestos sheets on either side of the sheets to be welded prevents loss of heat. The low melting point combined with the lack of color change makes it difficult to tell when the metal is ready to weld. Too much heat causes distortion and may even burn holes in the sheet. The weakness of aluminum when heated often makes it necessary to support the parts

F. C. Geibig, Development Laboratories, Linde Air Products Co.

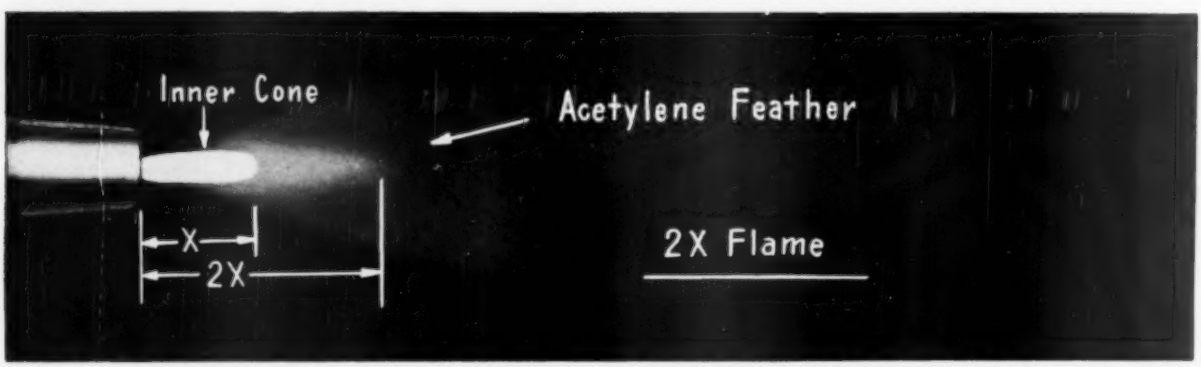


Fig. 1. Use an excess acetylene flame for welding aluminum. Adjust the flame so that the acetylene feather is about twice the length of the inner cone

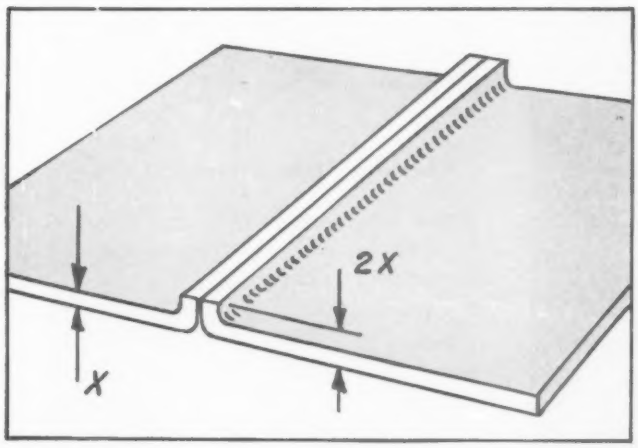


Fig. 2 Make a flange-type joint for thicknesses up to 16 gage. The flange should be about the same height as sheet thickness

Fig. 5 Paint the cleaned edges with aluminum flux mixed with water to a creamy consistency. This enables oxides to be removed easily

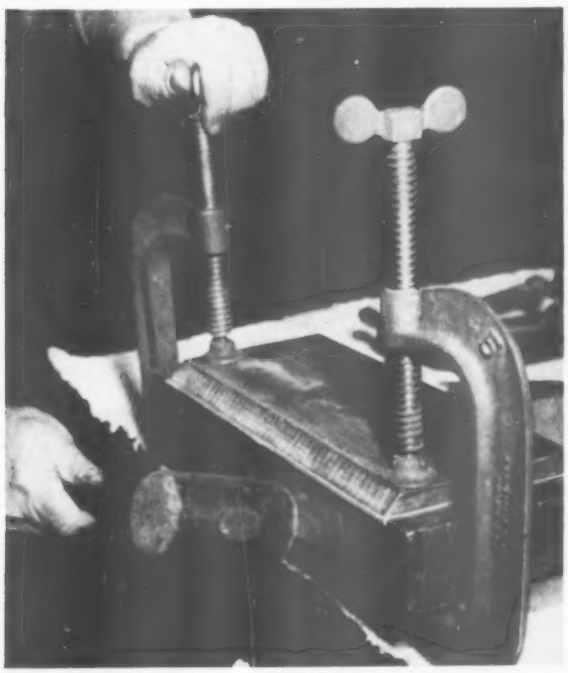


Fig. 3 Sheet edges can be flanged by clamping the sheet securely and hammering down the projecting edges with a wooden mallet

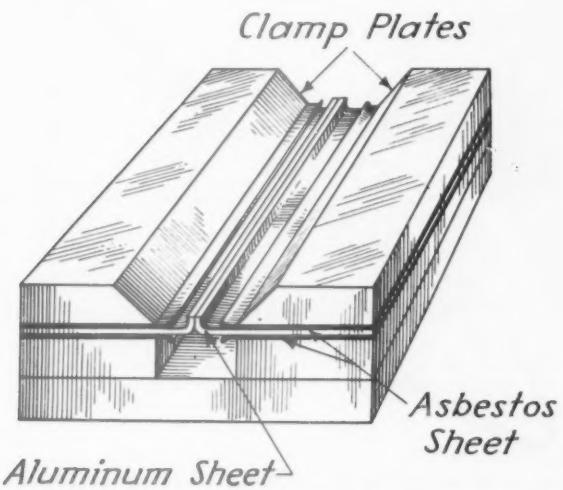


Fig. 6 This sketch shows a simple jig which clamps both sheets securely for welding. The asbestos sheet helps concentrate welding heat

Fig. 4 Clean the edges thoroughly until you get a dull-white, non-metallic surface. Steel wool or a wire brush and water will do a good job

throughout welding to prevent distortion. Carbon blocks, paste or rods, asbestos paper, c-clamps or a vise are some of the materials that can be used for support.

Aluminum oxide gives aluminum its resistance to corrosion. It forms a heavy, thick skin on the surface of the metal with a melting point higher than that of aluminum itself. This oxide can be removed by using an aluminum flux which combines with it to form a fusible slag that floats on the weld puddle. During welding this slag can be removed with the

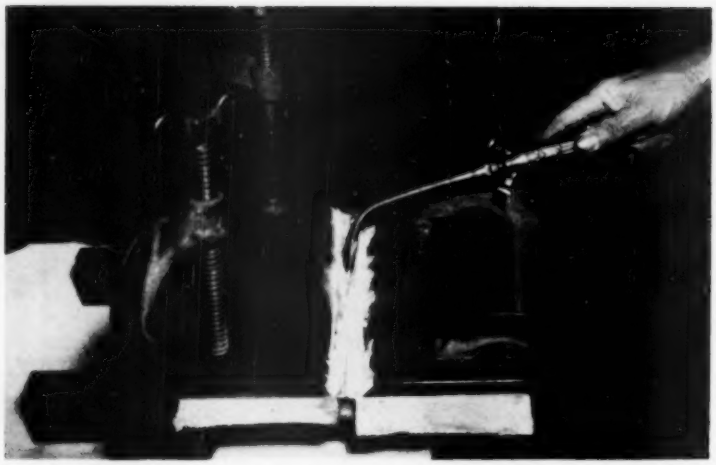


Fig. 7 Hold the inner cone of the flame about 1/8 in. from the metal. Weld rapidly and complete the job in one pass

Fig. 8 Here is a handy paddle for removing oxides and other impurities during a welding. Just flatten the end of a piece of steel welding rod

paddle made from welding rod. Figure 8 illustrates the paddle.

Preparation for Welding

All grease, dirt, oil and most of the aluminum oxide should be cleaned from the metal. Cleaning aluminum is an easy job with steel wool or a wire brush and hot water. A caustic washing compound can be used to remove grease or oil. All surfaces to be welded and the tops of the sheets for an inch back of the edge should be cleaned thoroughly.

Preheat the weld zone to a temperature just below the melting point. This heat will dissipate throughout the metal and lessen the chance of heat distortion when the higher heat of welding is applied. Remember to keep the preheating temperature below 700° F since higher temperatures will weaken aluminum alloys and may cause larger parts to collapse under their own weight.

Checking Preheat Temperature

There are five ways of determining preheat temperatures for those shops not equipped with pyrometers.

1. Write on the heated aluminum with carpenter's blue chalk. At proper welding temperature the chalk marks will turn white.

2. Special crayons, called Tempilstiks, can be used to make a mark on the metal before heating. This mark melts when the correct preheat temperature is reached. Your welding supply dealer can supply you with these crayons.

3. Touch heated aluminum with 1/8-in. wire half-and-half solder. At the right welding temperature the solder will melt easily.

4. If a pine stick is rubbed on aluminum that has been heated to the right temperature for welding, it will leave a char mark.

Fig. 9 Remove all flux after welding so that it does not corrode the aluminum. Use a wire brush and water to give the sheets a good scrubbing

Fig. 10 Here is the finished weld. Notice that it is even in width with regular ripples on the surface. There is no distortion because of the jigging

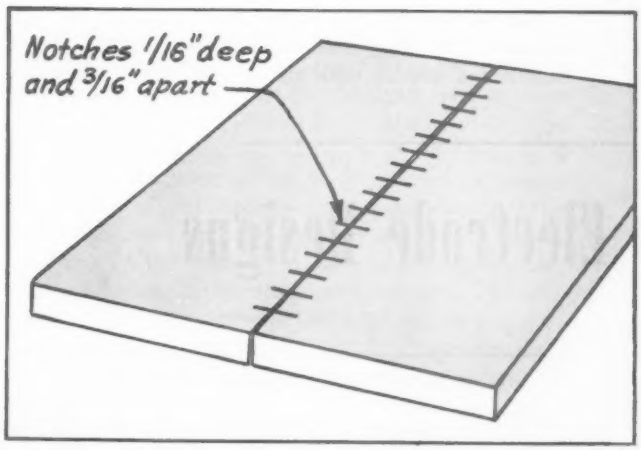


Fig. 11 For sheets from 16 to 11 gage in thickness, a plain butt-type joint with notched edges should be used. Notches should be $^{3}/_{16}$ in. apart and $^{1}/_{16}$ in. deep

Fig. 12 You can make notches in sheets by hammering a cold chisel with a mallet. Remember to clean, flux, and jig the sheets before welding

5. Cold aluminum gives off a metallic sound when struck. This sound becomes duller as the metal is heated until there is no metallic ring at welding temperature.

Control of the Weld Puddle

It is a good idea to practice heating and welding on scrap aluminum to get the feel of the weld puddle. Always use an excess acetylene flame. For a beginner, the excess acetylene feather should be about twice as long as the inner cone.

Hold the flame at an angle of 30 deg or less to the surface of the metal. This allows the heat of the flame to preheat the metal ahead of the weld puddle. Too steep an angle directs too much heat on the metal and might cause holes to be burned through the sheet. Notice how long it takes before a puddle forms in different thicknesses of aluminum. Practice puddle

Fig. 13 In addition to painting the sheet edges with flux, the welding rod should be similarly treated. This gives double protection to the weld puddle

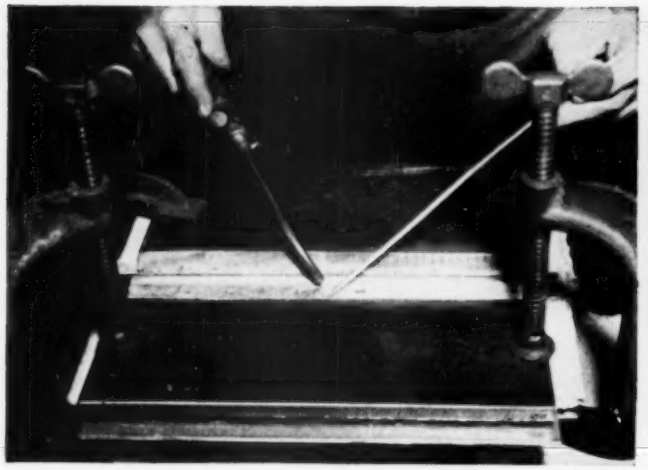


Fig. 14 During welding, hold the welding rod in a direct line with the weld. The end of the rod should be in the puddle near the flame

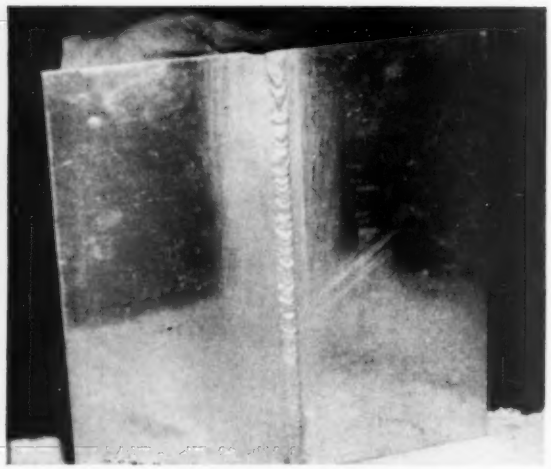


Fig. 15 Here is the finished job. This weld has regular ripples, is even in width, and shows no distortion just like the other weld

control until you can move the puddle across the sheet and get good penetration to the bottom of the welded joint without burning a hole.

The pictures in this article show step-by-step procedure for welding aluminum sheet of the common commercial grades with the oxy-acetylene blowpipe.

Powdered Iron Invades Electrode Designs

by L. K. Stringham

HEAVILY coated electrodes containing large quantities of powdered metal in their coatings obtain increased welding speeds on the order of 50%, with appearance in smoothness and freedom from spatter almost equal to that obtained with automatic welding.

The development of electrodes with powdered metal in their coatings is a milestone for metal fabricating industries. Although these electrodes are still new, cost reductions already obtained in production welding indicate that welding procedures will shortly be completely revised to new standards. Powdered iron electrodes will obsolete existing electrodes like the Jet Plane has obsoleted propeller-driven fighter planes.

This is not an opinion but is a calculated engineering conclusion. The basic fundamentals of the arc-welding process have led inevitably to the development of powdered metal coatings and, therefore, inevitably secure their position in welding progress.

In general, as welding current increases, the speed of welding increases. The electric arc drawn from the end of a welding electrode performs the three functions of melting the core wire, melting the coating of the electrode and melting the edges of the parts to be welded together. An increase in welding current increases the speed with which the arc can perform these functions. For any given size electrode, welding speeds can be increased until a maximum usable current is reached. Beyond this maximum current a further increase causes difficulties which result in unsatisfactory operation. The difficulty may be an overheated electrode causing a breakdown of the coating, it may be too much penetration, gouging of the parent metal or too much spatter. In any case, the effect is to impose the top limit on welding speed.

Electrodes with powdered metal coatings are designed to raise this top limit on welding speeds by eliminating or reducing the effect of these difficulties.

Table 1—Typical Currents and Speeds

Electrode size, in.	*Current, amp*	*Polarity*	*Fillet size, in.*	*Arc speed, ipm*
5/32	225	AC	7/32	15
5/32	200	DC(+)	7/32	12.5
3/16	275	AC	1/4	15
3/16	250	DC(+)	1/4	12.5
7/32	300	AC	9/32	15
7/32	270	DC(+)	9/32	12.5
1/4	350	AC	5/16	14
1/4	300	DC(+)	5/16	12

L. K. Stringham is Chief Engineer, Lincoln Electric Co., Cleveland, Ohio.

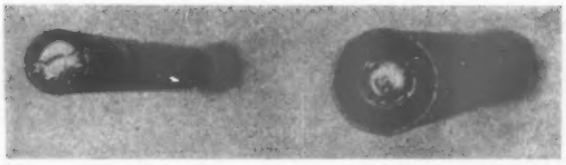


Fig. 1 Partially used conventional electrode (left), electrode with powdered metal coating (right) show difference in thickness of coatings and how crucible forms in the end of the thick powdered metal coating

This gives automatic control of mechanical arc length since drag technique is used in operation and controls arc action to give a smooth steady arc.

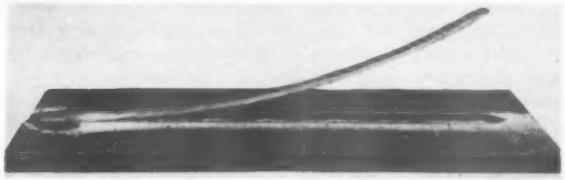


Fig. 2 Slag formed from powdered metal coating is practically self cleaning. This slag has lifted from the bead by itself as it cooled

The major cause of operating difficulties that limit welding speeds is the fact that the welding arc normally creates more heat than can be effectively used by conventional electrodes in melting the parent metal, the core wire and the coating. This excess heat usually is expended in melting an excessive amount of parent metal. The arc force throws this excess and some of the melted core wire out of the molten pool. The result, depending on the application, may be too much penetration, gouging, undercutting and spatter. The correction for the difficulty with conventional electrodes is, of course, to cut back on the amount of current used and slow up welding speeds until satisfactory operation is obtained.

Electrodes with powdered metal coatings use the excess heat available in the arc to increase welding speeds. The powdered metal in the coating is melted by this heat and becomes an additional source of metal for the weld, thus permitting an increase in welding speeds. The difficulties of excess current, spatter, gouging and undercutting are minimized and additional metal is available to make full welds at the higher speeds. If an electrode contains sufficient iron powder in the coating to supply one-third of the deposited metal from the coating and two-thirds from the core wire, that electrode can deposit metal 50% faster than the same size electrode without iron powder in the coating. Proper balance is necessary between coating and core wire, for if too much iron powder is put into the coating, so much of the heat of the arc may be absorbed by melting the coating that there will not be enough to melt the parent metal and core wire.

In addition to reducing spatter, undercutting and gouging, this more efficient use of arc heat by powdered metal coatings also reduces overheating in electrodes.

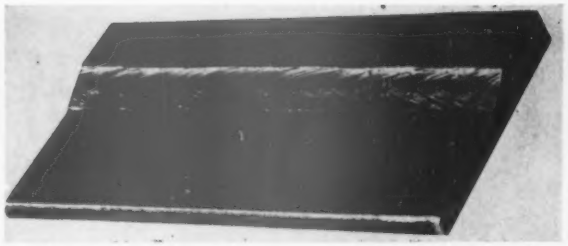


Fig. 3 Powdered metal coatings produce smooth clean welds comparable to those produced with automatic welding

This is a horizontal lap weld made in $^1/_4$-in. plate with a $^1/_4$-in. diam electrode. Current is 360 amp, arc speed 18 ipm! Conventional electrode, E6012, calls for 320 amp current and 12 ipm arc speed.

Fig. 4 Horizontal fillet weld, plate $^3/_8$ in., fillet size $^5/_{16}$ in., electrode size $^1/_4$ in. Current was 360 amp, arc speed 14 ipm

Overheating is an operating difficulty frequently encountered on welding applications where the rate of metal deposition is the only controlling factor on welding speeds. The temptation is to push welding current up to its maximum since current controls the rate of metal deposition. The danger is that of overheating the electrode to a red hot condition by forcing it to carry more current than its size warrants. A red hot core wire will break down the electrode coating so that it cannot satisfactorily perform its functions. The limit to welding speed, therefore, becomes the maximum current which can be used without producing a red hot core wire.

Powdered metal electrodes effectively raise the limit on welding speeds imposed by the current carrying capacity of the core wire. More metal is available for deposition and the more efficient use of the arc heat permits increasing welding speeds without necessarily increasing currents.

For the electrode consumer, the efficient use of arc heat by powdered metal coatings means:

1. Lower welding costs through higher welding speeds.
2. Lower cleaning costs because of less spatter and undercut.
3. Smoother and more uniform welds.

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activities • related events

Board of Directors Actions

There is given below a few excerpts from actions taken by the Board of Directors of the AWS at their meeting in Buffalo, N. Y., May 6, 1954. Other actions will be recorded in the September issue.

Report of Special Committee on Membership Classification

The Special Committee on Membership Classification, Dr. G. V. Slottman, Chairman, submitted an interim report which was identified as Agenda Item H and mailed to all Members of the Board of Directors on April 27, 1954. Dr. G. V. Slottman, Chairman, orally reviewed the important items of the report, observing that several schools of thought on Membership Classification existed in the order following:

(*a*) One group thought that there should be one grade of membership.

(*b*) Another group believed that a greater distinction should be made between the Member and Associate Member Grade classifications.

(*c*) The SOCIETY's Staff had presented a Membership Classification plan which was based on the Staff's surveys made of a number of Section Officer opinions throughout the country and which bridged a number of differences in thinking and, nevertheless, presented a compromise, combined with an arrangement whereby a low-priced Junior Membership classification would be created and up-grading would occur after specific periods of time; further providing for Fellow Membership in the Society as a reward of achievement and effort of any character—technical or administrative.

J. R. Stitt, Chairman of the 1953–54 National Membership Committee, was requested to review the proposals of the Staff and, after doing so, he advised that he favored the proposals made by the Staff as such would eliminate some of the difficulties now experienced. Dr. Slottman believed that the Staff's plan showed good promise. Mr. Howard still favored a single membership classification. Mr. Blankenbuehler, Chairman of the Section Advisory Committee strongly urged that whatever plan was adopted, such should include provision for reinstatement fee. It was generally agreed that it would be advisable to submit all of the proposed plans to the Section Management groups throughout the country, through the Section Advisory Committee; such proposals to also include the benefits that would be provided with each of the proposed membership classifications.

Action: Upon motion, duly seconded, the interim report of the Special Committee on Membership Classification was accepted and the suggestion that all proposals be submitted to the Section Management groups, such action to be initiated through the Section Advisory Committee, with the inclusion of benefits accruing to each member under the proposed membership classifications, was approved.

Interim Report of Section Advisory Committee

The Section Advisory Committee, J. H. Blankenbuehler, Chairman, presented its interim report. He advised that new Sections were now being formed in Appleton, Wis., Marion, Ohio, Long Island, N. Y., and Albuquerque, N. Mex. There were also several new Divisions being organized of the Houston AWS Section. He advised that the activity of his Committee had included the establishment of District and Section boundaries. The only open territories remaining were the States of Florida and South Carolina.

Action: Upon motion, duly seconded, the Board approved of the recommendations of the Section Advisory Committee in determining District and Section boundary lines, as recorded at this meeting and entered in the Section Advisory Committee's report.

Report Special Committee on AWS Housing

The Special Committee on AWS Housing, J. J. Chyle, Chairman, submitted their report. In discussion, Mr. Chyle stated that some SOCIETY Members would prefer a centrally located area rather than the existing one. In discussion, among Board Members there appeared to be a strong feeling that we need not join with the United Engineering Trustees, if such organization located outside of New York. However, if they relocated within New York, we might be inclined to tie-in with them but then would require more specific information regarding such arrangement.

The foregoing discussion, after review resulted in the following action:

Actions: (*a*) A check-ballot of the attending Members in regard to the feeling for locating within or without New York resulted in nine affirmative for remaining in New York and one negative.

(*b*) It was moved, and duly seconded, that a questionnaire be submitted to all Board Members which would include a review of the points raised and general feeling at this meeting, and questionnaire text also include the feelings of Staff Members regarding relocation.

(*c*) It was further advised that in the interim the SOCIETY would take over additional space in the Engineering Society's Building, approximately 725 sq ft, which has been vacated by the Society of Naval Architects and Marine Engineers, such space alleviating some of the crowded conditions which the Headquarters is now encountering. The additional space will be vacated by same June 1st and reconditioning to suit AWS requirements will then proceed.

Report of Special Committee on Awards

The Special Committee on Awards, H. Malcolm Priest, Chairman, submitted an interim report. He felt that his Committee should explore the subject of awards more completely, particularly in view of the recommendations made by the SOCIETY's Secretary under Agenda Item L in that the Secretary's proposal might very well provide a method for financing a SOCIETY wholly sponsored award activity of a nonproprietary nature. In discussion, the SOCIETY's Secretary advised that he had received a number of direct comments from Members that the Meritorious Awards, at the District level, as recently approved by the SOCIETY and which will be included in the new By-Laws when approved by the Membership, should not be confined to one award each year in each District. There seems to be considerable thinking that they should be extended to one award each year for each Section. H. W. Pierce observed that, in his opinion, it should not be automatic that each Section receive an award each year but rather that they should be based on merit and, in some cases, Sections might go several years without a Meritorious Award being given. The Board agreed that this Committee should continue with its exploration and, at the close of its work, present its final report.

Creation of Engineering Education and Awards Funds

Under the listing of Agenda Item L, the Agenda Attachment L was submitted to the Board of Directors with the Agenda for this meeting, mailed on April 21, 1954, to all Members of the Board of Directors. Such consisted of the SOCIETY's Secretary proposing the following:

(*a*) The institution of an Engineering Education Fund.

(*b*) The institution of an AWS Awards Fund.

(*c*) The appointment of a Special Committee on Institution of Engineering and Educational AWS Awards Funds.

The SOCIETY's Secretary orally reviewed the important points of the recommendations, pointing out that AWS should

(*d*) Recognize the need for comprehensive engineering educational program and ways and means for financing;

(*e*) Recognize the need for a comprehensive AWS Awards Program and ways and means for financing;

(*f*) Appoint a special committee to study the problem, the Secretary's proposal, other proposals of their own or others, and with scope in the order following:

"The study of suitable program for procurement of funds for sustaining a comprehensive engineering and education AWS Awards program, such to be reviewed and approved by the Finance, Reserve Funds, Education and Awards Committees, prior to bringing such recommendations to the Board of Directors, which recommendations shall comprise of proposed ways and means and method of operation."

It was suggested that the Membership of this special committee on institution of engineering education and AWS awards funds, consist of membership from the Board of Directors, the Finance Committee and the Chairman of the Reserve Funds, Education and Awards Committees.

Extension of AWS Activities

The SOCIETY's Secretary advised that there was nothing in the By-Laws or Constitution of the SOCIETY, nor its Certficate of Incorporation, which would indicate that it could not extend its activities to other countries in the North and South American Continents as well as U.S. Possessions. He pointed out that the name AMERICAN WELDING SOCIETY was all inclusive, and, in so far as the *name* was concerned, such did not confine operations to the United States only. He pointed out that Clause No. 3, in the Certificate of Incorporation, stating "The territory in which its operations are principally to be conducted is the United States of America —" is being fulfilled inasmuch as the central point of operations is in the U.S. The same clause also stated "—with membership privileges also to residents of foreign countries." Membership privileges, according to the By-Laws, includes organization of Sections. Accordingly, he recommended that the establishment of AWS Sections be permitted in both American continents and all U.S. Possessions, operating similarly to U.S. Sections, but without obligation of the National Society to assure speaker appearance.

Action: Upon motion, duly seconded, the Board approved the extension of AWS activities as recommended.

Employment Service Bulletin

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NEWS OF THE INDUSTRY

Convention Dates

The American Institute of Steel Construction will hold its 32nd Annual Convention this year, October 25th through October 28th, at the Greenbrier, White Sulphur Springs, W. Va.

1954 ASTM Annual Meeting

Featured by 36 technical sessions at which were presented 110 technical papers covering various aspects of engineering materials, the 1954 Annual Meeting of the American Society for Testing Materials, held throughout the week of June 13th at Chicago, also included 600 meetings of the technical committees.

Seventy-two of the Society's technical committees reported at the meeting, with the result that 65 new specifications and tests were approved and revisions in 229 existing tentatives and standards were acted on. A total of 109 specifications and tests that have been published previously as tentative were approved for reference to Society letter ballot for adoption as standard. All of these new and revised standards will be published later in the year in the 1954 Supplement to the Book of ASTM Standards.

Square D Expands

A 19-acre site in Royal Oak, a Detroit suburb, has been acquired by the Square D Co. for a new heavy-equipment manufacturing plant and regional sales office, it was revealed recently by F. W. Magin, President.

Initial construction at Royal Oak—to provide 60,000 sq ft of floor space and involve an investment of approximately three-quarters of a million dollars—is the first phase of an extensive new Square D expansion program, according to Mr. Magin. The company has plans to manufacture for new markets, he said, as well as increase the production of established products.

Square D products range from small, mass-produced pressure switches and circuit breakers which can be held in the palm of the hand, to large switch-boards and control panels weighing many tons. The company's customers are equally varied—from home owners, building contractors, architects and engineers to industrial plants, machinery manufacturers, government agencies and public utilities. Square D sales last year totaled $58,966,000, an increase of 6% over 1952.

Jury Announced for Welding Awards

Dean James H. Sams, School of Engineering, Clemson Agricultural College, will serve on the Jury of Award for the Annual Engineering Undergraduate Award and Scholarship Program sponsored by The James F. Lincoln Arc Welding Foundation, Cleveland, Ohio. The Jury will meet August 16th through August 27th in Alanson, Mich., to judge entries from engineering students in engineering schools throughout the country. The entries are mechanical or structural designs in which arc welding has been employed to improve and reduce the cost of machinery or structures. The best designs will be awarded a total of $3500, with the top award $1250. Scholarship funds totaling $1500 will be awarded to schools in which the main awardees were enrolled. Awards will be announced at the beginning of the fall school term.

The program is sponsored every year by The Lincoln Foundation to encourage engineers to study the potential for improvement and cost reduction through welded design. Students are given an opportunity to demonstrate their engineering design ability by applying arc welding to a design of their own creation.

Other members of the Jury are Dr. E. E. Dreese, Chairman of the Jury and Head of the Department of Electrical Engineering, Ohio State University, and Professor Sidney Shore, School of Civil Engineering, University of Pennsylvania.

Hard-Facing Paste

A new all-purpose, hard-facing paste has been developed especially for hard facing thin blades such as plow shares, bean knives and disk blades.

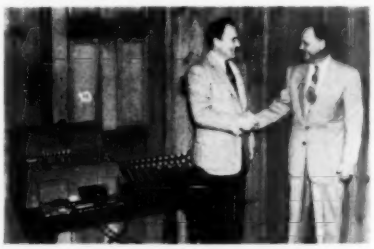
Developed and perfected after many months of research and field testing, the "Miracle Paste" as it is trade-named, provides the optimum in wear and impact resistance, as well as ease of application.

Spreading the paste on work is readily accomplished by any flat blade such as a spatula or putty knife. Thickness of deposit affects rate of wear only. Bonding application is by oxy-acetylene or carbon arc method. Normal time required to hard face most small blades is less than 15 min.

A generous free sample of Miracle Paste along with informative literature is available upon request to Mir-O-Col Alloy Co., 312 N. Avenue 21, Los Angeles 31, Calif.

Tangert Wins Contest

Eugene Tangert (left of photo), engineer at Ford Motor Co. Stamping Plant, Buffalo, is shown being congratulated by J. S. Manley, Buffalo District Manager of National Cylinder Gas Co. Mr. Tangert won the NCG guessing contest held during the meeting of the AMERICAN WELDING SOCIETY at Buffalo, and was



presented with a handsome tool box containing a complete set of socket, open-end, and box wrenches, valued at $170. Contest entailed guessing the number of times a ball-peen hammer, operated by a variable speed electric motor, hit the handle of a Torchweld "45" Medium Duty Torch, a new NCG product, during the three days of the show. Mr. Tangert's guess was only two hammer blows short of the correct answer—69,212. The primary purpose of the device was to demonstrate the ruggedness of the torch.

National Cylinder Acquires California Carbonic Co.

National Cylinder Gas Co. of Chicago has acquired the California Carbonic Co., Los Angeles, thus further augmenting its carbon dioxide business, Charles J. Haines, president, announced.

The outstanding capital stock of California Carbonic has been exchanged for 22,576 shares of National Cylinder common stock.

The Los Angeles firm sells carbon dioxide in both liquid form, which ultimately is used as gas principally for carbonated beverages, and in solid form as dry ice.

German Welding Committee

The work of the Welding Committee of the German Society for Ferrous Metallurgy during 1953 was summarized in the following way in their annual report. "Cooperative research was performed on the best method of testing mild and low-alloy steels for weldability, that is, susceptibility to brittle fracture. Many

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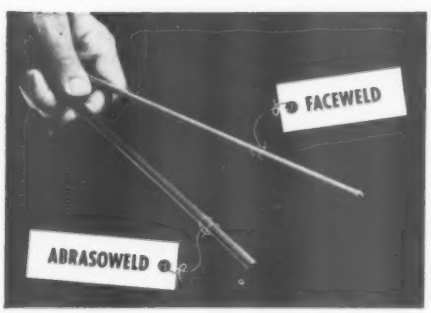

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Saves 39% *by production hardsurfacing of scraper blades using automatic Lincolnweld with Twinarc and Spreadarc. Service life . . . 3 times longer.*

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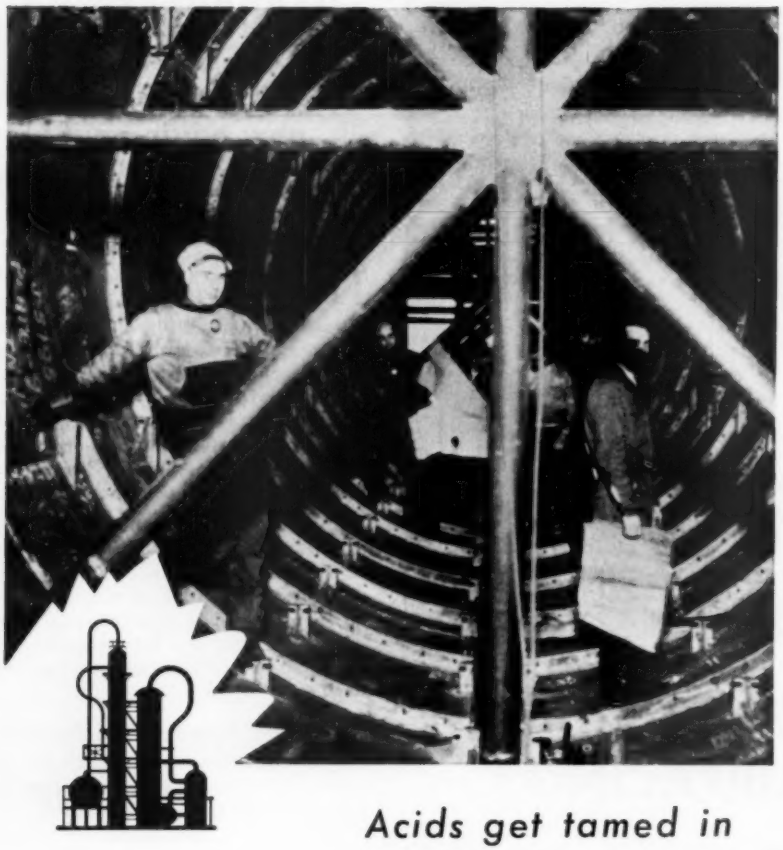

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STAINLESS RODS AND ELECTRODES

foreign technical papers and specifications show that it is very difficult to develop a satisfactory yet economical method in the adequate sensitivity and reproducibility. Nevertheless, the Committee believes it will be able to propose a preliminary test for the new DIN standards and German Railway specifications. The cooperative work has involved liaison with Committee 12 of the International Institute of Welding. The Committee has been working on the Society's Material Data Sheet 880—Core Rods for Filler Metal. The first draft has been issued jointly with the Electrode Assn. Further attention has been devoted to German Standards DIN 1910—Classifications of Welding Processes, and 1913—Filler Metal for Arc Welding."

Industrial Publishing Co. Sold to Telenews Productions; Hexter Remains as President

From Advertising Age—June 21, 1954

Telenews Productions, which specializes in TV film programs for industry and government, has purchased Industrial Publishing Co., Cleveland, publisher of *Industry & Welding, Industry & Welding Quarterly, Flow, Flow Quarterly, Applied Hydraulics, Commercial Refrigeration & Air Conditioning, Occupational Hazards* and *Precision Metal Molding.* All are controlled-circulation publications.

Announcement of the purchase—no price has been indicated—was made recently by Herbert Scheftel, chairman of Telenews. Associated with Mr. Scheftel in the ownership of Telenews are Marshall Field, president of Field Enterprises, Inc.; Conde Nast Publishing Corp.; Angier Biddle Duke, ex-ambassador to San Salvador; John M. Schiff, head of Kuhn, Loeb & Co.; Robert Straus and Alfred Burger, both of Telenews, and Paul Warburg and James Sachs, investors in Telenews.

This group, "impressed by the tremendous progress of trade and business magazine publishing in the last few years and the spectacular growth of several large business publishing companies," plans to use its acquisition of Industrial Publishing as "the nucleus for operations in this field."

It contemplates acquiring "several" existing magazines, and plans to bring out several other new business publications. At the same time, it was said, "the activities of the film company in industrial motion picture production have also increased sizably, so that the film and publishing operations can be correlated."

Industrial Publishing now becomes a division of Telenews. Irving B. Hexter, who, along with Lester P. Aurbach and Edwin M. Joseph, started Industrial in 1930 with a 24-page issue of *Industry & Welding,* will continue to direct publishing

activities. Telenews will continue in industrial film production and TV programming under the direction of General Manager Charles N. Burris.

Terms of the purchase agreement call for corporate officers of the combined company to be: Mr. Scheftel, Board Chairman Mr. Hexter, President; Mr. Burger (films) and Mr. Aurbach (magazines) both Executive Vice-Presidents; Mr. Joseph, Vice-President; and Mr. Straus, Secretary-Treasurer.

Present personnel with Industrial Publishing's Cleveland, New York, Chicago, Los Angeles and London offices will continue as before.

Mr. Scheftel is a brother of Stuart Scheftel, head of Young America Films, which recently sold the title Sports Illustrated to Time, Inc., for its new sports Weekly (AA, June 14th).

Pressure Vessel Tests

Coated with ice at 45 deg below zero, four large closed pressure tanks of a new kind of steel were put through dramatic burst and impact test paces in Birmingham, Ala., under a hot summer sun.

The vessels were made of a new quenched and tempered steel known as "Carilloy T-1." This new material is the product of United States Steel Corp.'s Research Laboratory.

The tests were staged in an open area adjoining the plant of the Chicago Bridge and Iron Co. Engineers and research experts of both companies took part in the series of tests before an audience of government and technical code authorities, representatives of the Atomic Energy Commission, army and navy officers, as well as the heads of military and civilian aeronautical groups.

"Operation T-1," as the test series was called, ticked off with the precision of a military maneuver, but behind the scenes long hours of hard work were required to bring the steel of the tanks down to the low temperature required. They were cooled by pumping into them hundreds of gallons of calcium chloride brine chilled by dry ice.

"Two separate test methods—burst tests and drop impact tests—were used to demonstrate the strength and toughness of the steel," said Leon C. Bibber, welding research engineer who headed U. S. Steel's special test force. Two burst tests were made on alternate days to determine how much internal pressure the tanks could withstand before they burst. In the drop tests a new method of testing full-scale vessels was used. A 26,700-lb steel weight was left fall upon the tanks from heights of 52, 73 and 101 ft to show the toughness of the steel at low temperatures. Internal pressure in the drop tests was 1875 psi.

Outwardly, when ready for the tests the four tanks appeared identical—20 ft

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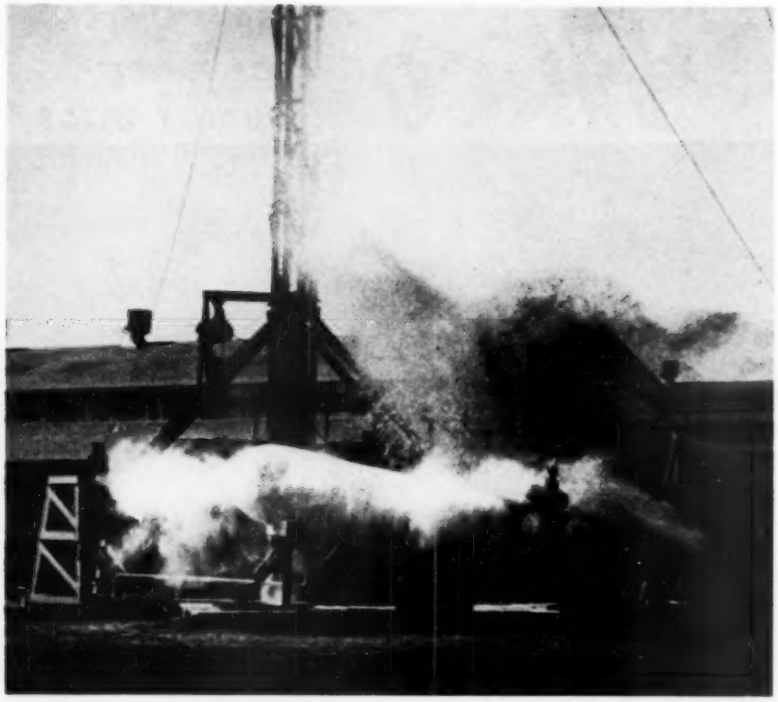
ALUMINUM AND STAINLESS SPOOLED WIRE



Geyser of icy calcium chloride brine rises as big steel tank, frozen at 45 deg below zero, is burst from internal pressure of 2850 psi. Steel is a new type developed by United States Steel Corp. Burst test was one of a series made on Chicago Bridge & Iron Co. plant site in Birmingham, Ala.

long, including their hemispherical ends, and 48 in. in diam, made from steel plates $^1/_2$ in. thick, crusted with white frost, but the two tanks tested the first day were not "stress relieved," that is, they were in the natural condition, as-welded. Those of the second day's tests had been "stress relieved" or given a special heat treatment following their fabrication so that internal stresses caused by the fabrication and welding were minimized. The objective was to compare how both kinds of tanks would react to the same tests.

Results of the tests performed on the four tanks were as follows:

1. June 29th: Burst test nonstress-relieved vessel at 45 deg below zero, 2850 psi internal pressure at moment of burst.
2. June 29th: Drop test nonstress-relieved vessel at 45 deg below zero, 26,700-lb weight dropped from 101 ft caused break.
3. June 30th: First test on stress-relieved vessel at 45 deg below zero, 2850 psi internal pressure at moment of burst.
4. June 30th: Drop test on stress-relieved vessel at 45 deg below zero, 26,700-lb weight dropped from 101 ft caused break.

At intervals throughout the test periods girth measurements of the tanks were made and recorded to show swelling and deformation.

Results of the burst and drop tests were spectacular. The steel showed tremendous toughness even at the very low test temperature. Yellow calcium chloride brine gushed like a geyser from a hole burst in the tank.

When the drop tests were completed the tanks had amply demonstrated their ability to withstand tremendous shock.

Cooper Alloy Expands

To meet the increased demand for ELC grade, the extra-low-carbon stainless steel which permits welding without subsequent heat treatment, the Cooper Alloy Foundry Co., Hillside, N. J., has expanded capacity by more than six tons of melted metal per day. According to J. L. Lessman, Foundry Superintendent, the demand for ELC stainless has been sparked by revived interest in the cast-weld method of construction, and by the increasing number of chemical process vessels which require field fabrication. Since this low-carbon grade permits welding without subsequent heat treatment, fabrication in the field may be done without lessening the corrosion resistance of the stainless. In addition, competitive conditions have led to an emphasis on cost cutting, which in turn has highlighted the cost advantages of cast-weld construction, particularly where the method eliminates machining of large unwieldy structures.

Chicago Pneumatic Tool Co.'s New Texas Plant

Chicago Pneumatic Tool Co.'s recently completed $4,500,000 plant in Fort Worth. Tex., is now in full production on a complete line of oil-well drilling equipment for the petroleum industry.

Designed and constructed by Walter Kidde Constructors, Inc., engineers and builders of New York and Houston, the new 130,500 sq ft plant incorporates engineered facilities that make it an outstanding installation for its type and size. The design criteria of ample flexibility for current and future operations, low initial cost, and simplicity and economy of operation are reflected in both building construction and plant services.

The plant consists of three attached buildings; a 200- x 600-ft manufacturing building; a 35- x 200-ft office building; and a 36- x 67-ft cafeteria.

Fig. 1 Front facade has brick walls, attractive architectural treatment. Other walls are block, asbestos siding

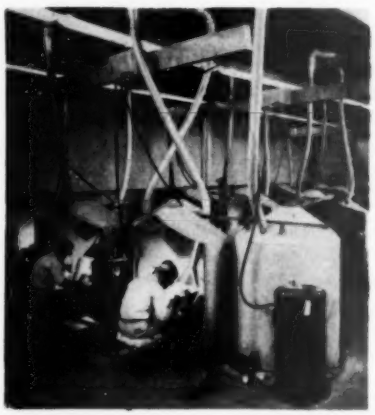


Fig. 2 Welding shop is air conditioned. Remainder of plant has efficient mechanical ventilating system

Because of the high heat load in the heat-treating area, it was isolated from the general factory area and provided with an independent supply and exhaust system.

An air-conditioning system with a capacity of 11,400 cfm at a supply temperature of 54 deg serves the numerous welding booths, where the heat output would otherwise be unbearably high. For cooling economy, exhaust ducts in each welding

nothing to stop you . . .

look — its sturdy, smooth, safely silver soldered tubes glide effortlessly through the operator's guiding hand. No obstructing coupling nut or tube joint hinders good performance, suggests the possibility of dangerous gas leaks. So many outstanding, modern and economy producing advantages have been incorporated into NATIONAL flame cutting torches that you, too, will find it to your interest and profit to write for the now rather famous brochure . . .

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booth remove a large portion of the heat right at the source.

Gas-fired, blower-type unit heaters provide winter heating.

Sewage Lines

Selection of a material that lends itself to low-cost maintenance per-year-of-service is a common consideration of engineers who specify for public works projects.

This is particularly true of sewage lines where the corrosive nature of the application and the inaccessibility of the piping are two factors that influence maintenance expense.

To help avoid unnecessary costs due to prematurely failed pipe in huge sewage lines serving Bay Village, Ohio, Cuyahoga County Sanitary Engineers selected corrosion-resistant wrought iron. The pipe—size 48 in. diam—is installed in approximately 270 ft of the Bay Village sewage system.

More than 37 tons of $^1/_2$-in. wrought iron plate was required by Morgan Steel Products, Inc., to fabricate the large pipe in their Cleveland (Ohio) shop.

The seven segments of the Bay Village sewage line are each welded by the manual arc process on the outside with three passes of E6012 electrodes ($^3/_{16}$-in. rod) to a depth of $^1/_2$ in. There is a reinforcing weld in the interior of the pipe.

"Heat's on" here as welder Robert Cebulak, Morgan Steel Products Incorporated, Cleveland, O., works on the interior of one segment of the corrosion resistant wrought iron pipe slated for installation in the sewage line of Bay Village, Ohio.

Welds were checked for compliance with the standards of the AMERICAN WELDING SOCIETY by the James H. Herron Company Laboratories, Inc., Cleveland, as required by the Cuyahoga County Sanitary Engineer, John H. Puzenski and Assistant Engineer C. Riley Westmoreland.

Installation of the sewage line piping was handled by the Utilities Construction Co., Cleveland.

Welded Steel Pins Simplify Insulation of Huge Sphere

Insulating the huge sphere to be used by the Atomic Energy Commission and General Electric Co. for testing an atomic submarine engine, presented some unique problems.

The sphere, 26 times larger than any other previously built, is 225 ft in diam and has a content of $5^1/_2$ million cubic feet. It is designed to minimize escape of fissionable material during the atomic engine tests near Schenectady, N. Y.

In effect, the sphere is the largest "thermos bottle" ever built. It is airtight and insulated with "Foamglas," a cellular glass material developed by the Pittsburgh Corning Corp. More than 138,000 sq ft of this insulation—in blocks 2 in. thick, 12 in. wide and 18 in. long—were applied to the steel walls of the giant sphere.

Fig. 2 Problems of insulating the atomic engine test sphere were simplified by using cellular glass insulation blocks which were impaled on steel pins welded to the sphere's shell. (Courtesy of Pittsburgh Corning Corporation)

Fig 1 World's largest "thermos" bottle is this huge steel sphere for testing atomic submarine engines. (Courtesy of Pittsburgh Corning Corporation)

Don't replace . . . REBUILD with an AMSCO® LEADER automatic hardfacing machine

With an Amsco Leader automatic hardfacing machine, you can save a great deal of money. Let's take a look at a typical example:

If you operate five or more large track-type tractors—in construction, logging, oil fields or other industries—you will benefit from these typical savings. On each of these tractors you have 12 bottom rollers, 4 carrier rollers, 156 track links, 78 grouser pads, 2 idlers, 2 sprockets and one dozer blade. Their total replacement cost for five tractors would be approximately $13,800.

To rebuild these same parts, using an Amsco Leader automatic hardfacing machine, will cost approximately $3,700, or a saving of approximately $10,000, plus the fact that you will get *increased wear life.* This is a typical example. Your local Amsco Distributor will be glad to furnish others.

The complete versatility of this machine allows it to be used for automatic mild steel fabricating, manual welding, semiautomatic submerged or open arc welding.

For additional specifications or information on applications, contact your local Amsco Distributor, or write Amsco Division, Chicago Heights, Ill.

Brake Shoe

AMERICAN MANGANESE STEEL DIVISION
Chicago Heights, Ill.

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The blocks were impaled on steel pins $2^1/_2$ in. long, welded to the side of the sphere on 9-in. centers. Two pins support each block. After each block was impaled, metal clips were attached to the pins and recessed about $^1/_{16}$ in. into the insulation. The protruding end of the pin then was clipped off.

Special scaffolding, supported by permanent welded steel lugs on the sphere wall, was used to install the insulation.

Longest Crane

The longest crane ever built by the Cleveland Crange & Engineering Co., Wickliffe, Ohio, has just been completed and shipped to a large steel mill.

The bridge girders have an over-all length of 141 ft and weigh 33 tons each. They are of the all-welded steel construction.

The crane has a capacity of 10 tons and is of the double-leg gantry type. It will be equipped with a magnet and be used for handling pig iron.

Swimming Pools

Based on experiments over an 11-year period, the American Bridge Division of United States Steel Corp. is now marketing all-steel swimming pools in a range of four sizes.

Development of the all-steel pool is the answer to a number of basic cost, construction and maintenance problems long associated with conventional swimming pools. Property development men report the new pools as "the best real estate salesmen to date." Citizens' and veterans' groups, community and country clubs as well as private home owners have found the new all-steel pools the answer to summer family pleasure at comparatively low cost.

A large resort development at Hollywood, Md., featured the new U.S. Steel pool in full page newspaper advertisements. Propective buyers were invited to inspect the property and use the pool. As a result of this sales technique, nearly a third of the lots were sold in the first month of the sales program.

An all-welded steel panel construction, the new U.S. Steel product boasts many features not found in ordinary pools. For example, the pools are surprisingly low priced and constructed of corrosion- and rust-resistant, copper-bearing steel throughout. Paint requirements, long a plague of conventional pool maintenance, are reduced to an occasional coat of paint only, for maximum beauty and care. The pools are leak- and seep-proof when installed and they stay that way. Frost action and other problems in any climate are obviated by the design and construction of the steel pool.

Simply described, the all-steel pools now being fabricated and installed by the engineers of American Bridge have bottom and side plates just like bridge girders, designed for simple assembly in the field. The sections of steel, shipped from American Bridge Division plants to construction sites, are designed with special features for easy adjustment as to level and line. Except for small splash and evaporation losses, the pool needs to be filled but once, since each unit is completely adaptable to filtering and recirculating purification systems, providing clear water in constant circulation.

Pools of four standard dimensions are now available in sizes ranging from 33-ft lengths by 16 ft wide to the 105-ft "Olympic" size pool, which measures 35 ft wide. Special dimensions, of course, can be constructed to meet individual or group requirements. While individual panels or sections are welded together at the construction site, American Bridge engineers have designed these pools for quick assembly This added and desirable feature is attained primarily by keeping the weight of the individual sections to a maximum of a ton or less. This lower weight per section feature permits quick field assembly by a small force. With members set in place, positions are provided to support splash drains and to provide a method of placing the top edge of the pool level and straight. The sides and ends of this design attach to the bottom plates for proper positioning and added strength.

While this is the first full sales and production season for the steel pools, remarkable results have been reported from several installations throughout the country. However, years of trouble-free pleasure, with low maintenance costs, are yet to come for owners of the new U.S. Steel swimming pools.

Institute of Welding Elections

The Institute of Welding of Great Britain has recently held an election of officers and has announced the results as follows:

President—R. G. Braithwaite, MICE
Vice-President—R. E. G. Weddell
Honorary Treasurer—W. E. Harriss

NEW LITERATURE

Hardsurfacing Weld Rod

The availability of a booklet on the use of No. 170 Hardsurfacing Weld Rod has been announced by Coast Metals, Inc., of Little Ferry, N. J. Copies may be had by writing (directly) to the company.

Fluxes

Alpha Metals, Inc., 56 Water St., Jersey City 4, N. J., has just issued a 4-page circular giving technical and other information in regard to their solder flux line.

Booster Bulletin

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., have issued Bulletin No. 322-10, covering the Sciaky Hydro-Pneumatic Boosters, designed for operation with ordinary water, and which can be used with same success with oil or noninflammable hydraulic fluids.

Avoid fire hazards—avoid safety hazards with Sciaky Water Hyro-Pneumatic Boosters, a fireproof pressure medium without the expense of noninflammable hydraulic fluids.

Sciaky Booster operates with water—water is far less expensive than oil or noninflammable liquids; water eliminates messy or dangerously slippery work areas; water completely eliminates the fire hazard of oil. Normal plant water line or welder cooling line provides sufficient supply for Sciaky Boosters.

Write for Bulletin No. 322-10 which describes the advantages in the design and operation of Sciaky Boosters.

Practical Piping Flexibility Design and Flow

The above is the title of a loose-leaf book with flexible imitation leather covers published by W. A. Thomas, Consulting Engineer, 2742 Philadelphia Ave., Pittsburgh 16, Pa. The book sells for $25. The book includes tables of Viscous Liquid Flow and sections on Pumps, Pumping, Economical Pipe Size, and Thermal Stress in Plates and Vessels.

Also included in the book are sections on related subjects such as Flow of Liquids; Pumping Data; Flow of Steam; etc. Much thought has been directed to obtain original treatment of these subjects, so that the reader may gain in "know-how" and facility in the work. Mathematical logic has been followed in the construction of the many short cuts so far as compatible with experience and test.

The book is copiously illustrated with charts, formulas, tables and includes an index. It consists of 190 loose-leaf pages.

Welding and Cutting Catalog

A new *Welding and Cutting Equipment Catalog* has been published by the Liquid Carbonic Corp. which lists the company's complete line of Gasweld equipment. This catalog, designed for easy selection, offers equipment for use in the entire "flame" field from welding and cutting to heat-treating operations.

Copies are available free of charge upon request to Liquid Carbonic Corp., Advertising Department, 3100 S. Kedzie Ave., Chicago 23, Ill.

Welding Catalog

Bulletin No. WE-166, the first stainless steel welding electrode catalog produced by the Welding Products Division of the A. O. Smith Corp., Milwaukee, Wis., has just been printed. It is particularly useful to those engaged in welding of stainless steels.

The 24-page booklet covers in detail the metallurgy of stainless steels, provides an A. O. Smith stainless steel electrode selector table and offers other valuable tabular material on welding processes as related to varying chemical composition involved with types of stainless steels.

Each of the A. O. Smith welding electrodes available for stainless steel work is described in detail as to typical mechanical properties of deposited metal, chemical analysis range of deposited metal and recommended amperage and voltage range.

Filing System for Welding Supply Distributors

Associated Business Counselors, Box 112, Skokie 3, Ill., announce a Master Welding Supply Index and Index Insert set (copyrighted) for welding supply distributors. The Master Index and the Index Inserts are the first of their kind ever developed for the welding supply business. Together they provide an accurate catalog filing system so that every catalog will be in "the same place—every place."

The Master Index separates the products used in welding into 16 groups. Each group is divided into classes with as many as 18 class divisions in a single group. The 8-page Master Index, listing over 350 welding products, is Kalamazoo punched and may be found in any standard catalog binder for safe keeping.

K-G Catalog

The K-G Equipment Co., Inc., P.O. Box 538, Allentown, Pa., has issued a Catalog, No. 54E, consisting of 24 pages and covers, describing their cutting torches, cutting tips, welding torches and attachments, all types of welding tips, adaptors, regulators, outfits, and miscellaneous items. Copy available on request.

Regulators

The Victor Equipment Co., 844 Folsom St., San Francisco, Calif., has just issued a 12-page, attractive Bulletin No. 341 pertaining to Victor High Pressure and Corrosion Resistant Regulators. Copy available on request.

Report No. R.10 on S.S. "Ocean Vulcan" Sea Trials

This particular Report R.10 gives the detailed analysis of one of the more important records taken during the sea trials on the SS *Ocean Vulcan*. This Record, No. 6.E.7, was made when the ship was fully laden.

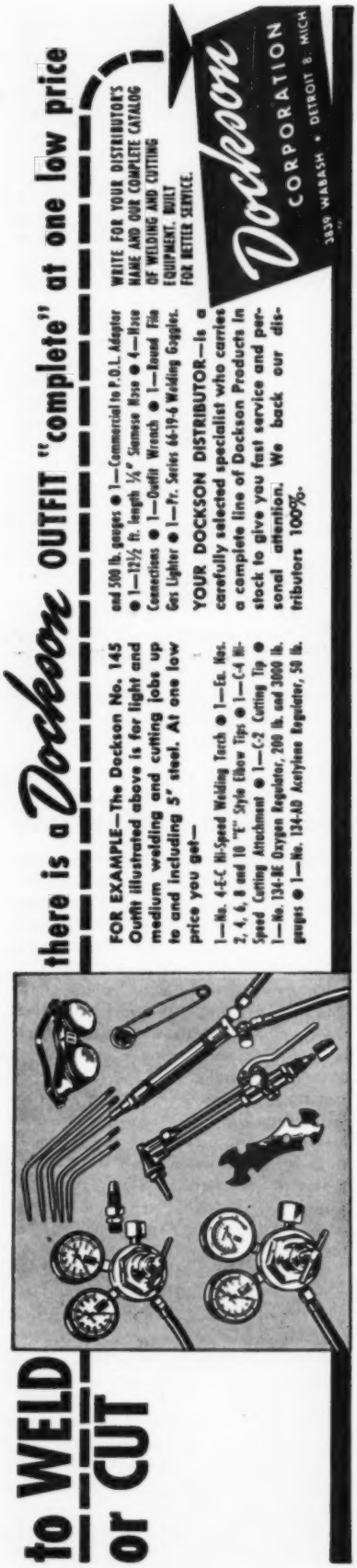
The general condition of loading of the ship and of the seaway which applied at the time are given together with extracts from the field data with a brief discussion of their significance.

Some of the more interesting cases selected from the detailed analysis are published in full in order to show the magnitude of the acceleration and bending moments, the variations in hydrostatic pressure and other actions along the length of the ship. An estimate of the probable accuracy of the results is made.

Envelope curves are given to show the maximum values of bending moment, etc., which were recorded along the length of the ship, and an investigation has been made to ascertain the combinations of the various actions which cause the highest stress amidships.

This report should be read in conjunction with Report R.8 wherein the nature of the trials is fully described.

The book is obtainable in this country from British Information Services, 30 Rockefeller Plaza, New York, 20, N. Y., price $9.00 postpaid.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Stud Welding Wall Chart

A handy, time-saving wall chart, intended to facilitate selection and ordering of welding studs, is included in a new "Coast Saver Kit" now available on request from the Nelson Stud Welding Division of Gregory Industries, Inc., Lorain, Ohio.

The wall chart lists part numbers and dimensions of the standard, available-from-stock Nelson threaded MG studs and ceramic ferrules. Special-purpose studs of a number of types are also illustrated and described briefly on the chart, as well as the equipment used in this split-second fastening method.

Also included in the kit are the manual, *How to Design for Nelweld*, and a *Nelson Stud Welding Catalog*. The kit comes in a special mailing tube that simulates a threaded stud.

Nickel-Aluminum Bronze

Bulletin A-133 is composed of 12 pages, with tables of properties and illustrations of applications. It describes a group of zinc-free, tin-free, high-strength copper-base alloys that possess high resistance to corrosion, erosion, cavitation and wear; and that may be heat treated like steel to increase their strength and hardness. Their toughness, weldability, workability and low specific gravity are discussed. Applications are presented for the marine, aircraft, chemical, electrical and other industries. Write International Nickel, New York 5, N. Y.

German Handbook

The newly revised, 28th edition of Part IIA of the comprehensive German engineering handbook *Hütte* covers the field of machine design and construction. It is divided into nine sections: (1) Elements of Machines, (2) Transmissions, (3) Dynamics of Machines, (4) Piping and Valves, (5) Steam Power Plant, (6) Piston Pumps and Engines, (7) Centrifugal Pumps, Hydraulic and Steam Turbines, (8) Machine Tools and (9) Regulators. Each section is divided into subsections written by different members of the Hütte staff.

Two subsections discuss welding. Section 1, I, D devotes 12 pages to welding symbols, fatigue factors for butt and fillet welds, good and bad design details, allowable stresses in welds for structural, pressure vessel and machine design, and design of spot, projection and seam welds. Section 8 VII (44 pages) describes the various cutting and welding processes with the aid of 25 tables, 52 figures and references to 113 technical articles and numerous welding codes. An amazing amount of essential, up-to-date information is compressed into the two welding sections.

The handbook is published by Wilhelm Ernst & Sohn, Berlin-Wilmersdoft, 1292 pages, 2024 figures, 406 tables The price is 25 marks.

Ductile Cast Iron

Bulletin DI-1 contains 12 pages with illustrations, graphs and charts. It describes a recently developed family of irons which possesses the process advantages of cast iron and which has engineering properties that approach those of cast steel. Strengths up to 120,000 psi, substantial ductility and resistance to heat and wear are reported. Potential applications in numerous fields are indicated. International Nickel, New York 5, N. Y.

Welding Positioners

A new descriptive bulletin on P&H Welding Positioners has just been made available by Harnischfeger Corp., Milwaukee. Fully illustrated, the bulletin outlines important features and gives condensed specifications on four different models with capacities up to 35,000 lb.

An efficiency graph based on typical welding jobs enables the reader to get a quick picture of production advantages. A section on attachments for special jobs is also included.

Copies may be obtained by writing the Welding Division, Harnischfeger Corp., Milwaukee 46, Wis. Ask for Bulletin PL-1.

Victor Regulators

The Victor Equipment Co., 844-54 Folsom St., San Francisco 7, Calif., has issued a new Bulletin 341 pertaining to Victor high-pressure and corrosion-resistant regulators. This book contains specifications and technical data with reference to the various types of regulators. Copy is available on request.

Air-Gas Torches

National Welding Equipment Co., 218 Fremont St., San Francisco 3, Calif., has just published an attractive pamphlet of 36 pages, illustrated in color, describing the air-gas torches of this company. Some of these torches have been developed as the result of intensive field research work with public utilities companies—changing from a manufactured to a natural fuel gas. Copy of this pamphlet is available on request.

Coast Metals No. 119

Coast Metals, Inc., announces the availability of their booklet F-1 on the use of Coast Metals Hardsurfacing and Wear Resistant Castings for maintenance and repair of foundry equipment. Copies of it

may be obtained by writing to Coast Metals, Inc., Little Ferry, N. J.

Residual Stresses in Metals and Metal Construction

This book is 363 pages, prepared by some 22 outstanding authorities and edited by W. R. Osgood, has been published by the Reinhold Publishing Co., 330 W. 42nd St., New York 36, N. Y. The book sells for $10.

Prepared under the guidance of the Committee on Residual Stresses of the National Academy of Sciences–National Research Council, this valuable book describes and carefully evaluates the effects of residual stresses upon the performance of various kinds of structures. Particular emphasis is placed on the possible contributions of these stresses to fracture.

Twenty-two papers, contributed by specialists in the field from both here and abroad, offer an amazingly wide range of information on the origin, magnitude, distribution, etc., of all types of residual stresses existing in a structure or a machine, whether arising from welding, machining, or other causes. In addition, a special summary prepared by the Committee covers many aspects of the residual stress problem, and includes several recommendations for future research and study.

All metallurgists, construction engineers, structural designers, in fact, anyone concerned with the strength and stress properties of metals and the many considerations involved—will find the information contained in this volume of exceptional value.

Airco Heliweld Catalog

A new 16-page catalog is being offered by Air Reduction covering in detail features of the Heliwelding process and the equipment used with it.

The catalog fully describes and illustrates in actual welding operation, the manual holders either air- or water-cooled, light- or heavy-duty water-cooled machine holders for use with the semiautomatic equipment and the automatic head that can be used with the Heliweld process. The automatic hard-facing process using vibratory feeder is also shown. The booklet tells how each piece operates, which type of work it is best suited to and why it is the choice for a specific job.

Accessory equipment such as control panel, filler wire feeders, control stations, electric arc welding machines and many other items are also shown.

Heliwelding is Airco's tungsten-arc-welding process which uses an inert-gas shield of helium or argon or both to protect the weld from contamination by the atmosphere. It joins light gages of the "hard-to-weld" metals rapidly, inexpensively and efficiently.

Request a copy of the catalog, ADC 709B, by writing to Air Reduction Sales Co., 60 E. 42nd St., New York 17, N. Y.

Thermal Diffusivity

Thermal diffusivity of two aluminum alloys, 3S and 61S, which are commonly used for welding purposes, shows little or no increase with temperature between 150 and 1000° F. This conclusion, of practical importance to welding aluminum, is the result of a new experimental procedure and instrumentation developed for metals of relatively high thermal diffusivity. It was given to the American Society of Mechanical Engineers at its semiannual meeting held recently in Pittsburgh, by D. Rosenthal and N. E. Friedmann, University of California.

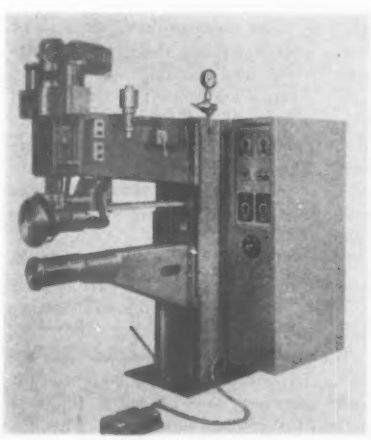
The method of moving heat sources and experimental procedure for high temperature is rapid and direct, the authors stated and the figures were easily determined to three significant places. Precision was estimated to be 2% on the basis of several consecutive runs, but since six consecutive runs can be made within two or three hours the attainment of the required precision is believed to be practicable. A complete, illustrated description of the experimental equipment and procedure was presented, including a discussion of results and such factors as hardening, alloying elements and the attainment of the quasistationary state.

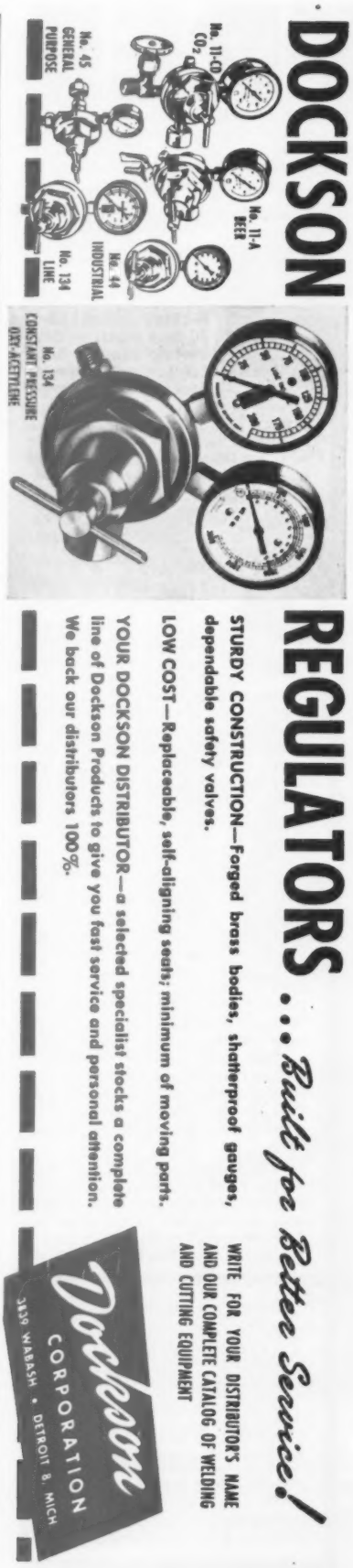
Copies of the paper, No. 54-SA-56, may be obtained at 50 cents each from Order Department, ASME, 29 W. 39th St., New York 18, N. Y.

Seam Welder

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., have issued Bulletin No. 321-7 covering the Sciaky MP 2 series, air-operated, press-type, low-impedance single-phase seam welder.

The Sciaky MP 2 machine is one of a series of single-phase seam welders availa-

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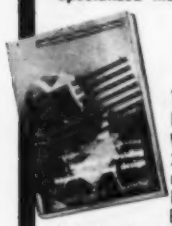

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MANUAL OF DESIGN AND WELDING ENGINEERING. 20-page digest of most useful, popular book Eutectic ever offered. Basic welding "know-how." Valuable to engineers, foremen, weldors. Suggests new, profitable uses for welding. Shows successful designs possible with "non-fusion" welding. Covers steel welding, all metals. Hints on weld inspection and control.

TIS 1010
TRUCK AND CAR FLEET MAINTENANCE MANUAL. 56 pages of pictures and text giving practical welding help on "101" automotive jobs. Tells how to salvage castings, repair shafts, transmissions, clutch and drive assemblies. Describes frame and spring repairs. Invaluable for saving time, money on body repairs and for wear resistant overlays. Every industrial, repair shop and garage weldor needs this book.

TIS 800
TOOL & DIE SALVAGE MANUAL. 68 pages of effective tool and die welding and salvage recommendations. Includes basic welding procedures. Covers cast iron, kirksite and shoe or "clicker" dies. Has useful tempering chart and fully develops unique ability of "non-fusion" welding to eliminate damage to base metals. Special sections on new methods of production welding tools, producing forgeable welds. Hundreds of hints for cutting tool costs.

All Eutectic literature is prepared from actual case histories showing how Eutectic Low Temperature Welding Alloys solve welding problems and make possible substantial savings which can not be realized with conventional materials.

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ble in 100, 150 and 200 kva, at 50% duty cycle. The Sciaky MP 2 can be supplied from 18- to 48-in. throat depth, and 1500 to 3000 lb max electrode force, dependent on throat depth.

The Sciaky MP 2 will make high-quality welds on large production runs on two thicknesses of clean mild steel in minimum gages of 0.021 in. and maximum gages of 0.109 in.—stainless steel in minimum gages of 0.031 in. and maximum gages of 0.109 in. By varying weld time, weld current and electrode force, heavier gages can be joined at slower speeds.

For complete specifications on the MP 2 and seam welding applications, write for Bulletin No. 321-7.

Electrode

Rocket No. 24, as the name implies, is Hobart's new high-speed welding electrode for horizontal and downhand welding. Best suited for faster welding on mild and medium carbon steel, this electrode is recommended for welding operations requiring AWS Classes E6012 and E6020 electrodes such as machinery, low pressure storage tanks, and light structural work.

This contact type electrode opens a new field for manual welding because of its high deposition rate with less physical effort in welding and less skill in operation required. Produces a high quality weld with a finely rippled bead approaching the appearance of a submerged arc weld. Spatter loss is exceptionally low and the slag is easily removed by a light tap. For free sample, write Hobart Brothers Co., Dept. RE, Troy, Ohio.

Welding Book

The latest developments in the science of welding aluminum are described by Aluminum Company of America in the completely new book, *Welding Alcoa Aluminum.*

In this 176-page book, the vast knowledge of Alcoa's Aluminum Research Laboratories is combined with the practical experience of its Process Development Laboratories. The company's experts spent over a year compiling the extensive material for this new manual.

Photographs and drawings are coupled with the comprehensive text to illustrate all the practical methods for welding aluminum. These include torch welding, arc welding—including inert-gas-shielded arc welding—resistance welding and pressure welding.

Special attention is given in the book to selection of welding method, performance of welds, welding of aluminum castings and control of welding quality.

After an introductory section that explains characteristics of welded joints, the book describes how to select the best method for welding aluminum. This discussion includes the factors that influence aluminum alloy selection for a welded structure.

Comprehensive descriptions of the methods for aluminum welding are presented in the six chapters that follow. Chapters are included on the following welding methods: inert-gas shielded-metal arc welding with both tungsten and consumable electrodes, spot and seam welding, flash welding, metal arc, carbon arc, atomic hydrogen and gas welding.

Welding of aluminum alloy castings is explained in the next chapter. This is followed by a complete discussion of pressure welding methods and applications.

One of the most important contributions of the new welding book is the section on performance of aluminum welds. This discussion offers the reader the extensive experience of Alcoa's research facilities in testing weld performance. Methods are outlined for designing parts with welded joints.

Careful attention is devoted to quality control and safety in the new welding book. A chapter is included on each subject. Quality control is discussed in terms of methods for testing aluminum welds, including visual inspection, pressure or mechanical testing, radiographic examination and metallographic inspection. The section on safety emphasizes methods and precautions that are important when welding aluminum.

In a final section are 32 comprehensive tables giving important data on welding aluminum. These tables include such important information as the physical properties of Alcoa alloys, alloy compositions and strengths of certain type welds. They also provide useful facts about welding conditions, with drawings showing edge preparation for most welding methods.

Welding Alcoa Aluminum replaces *Welding and Brazing Aluminum Alloys* in the company's How-to-Do-It series of books. Another book dealing specifically with brazing will be published at a later date.

Copies of *Welding Alcoa Aluminum* are available from Aluminum Company of America, 733 Alcoa Building, Pittsburgh 19, Pa.

Solder Flux

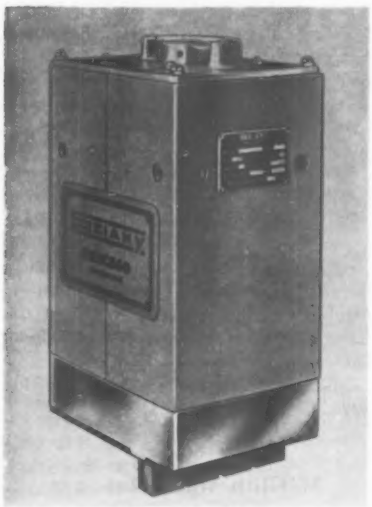
Alpha's new and extended line of external solder fluxes has been developed to afford a soldering flux for every purpose. These fluxes range from the mild noncorrosive rosin type to the strong highly active stainless steel type.

For the electrical, electronics and printed circuit industries a series of noncorrosive rosin-base fluxes are available. For general-purpose soldering, several active fluxes are available. All of these have water-soluble residues which can be removed by washing. A highly active flux for use on stainless steel, Monel, Nichrome, etc., is also available.

Write for Flux Bulletin, Alpha Metals, Inc., 56 Water St., Jersey City 4, N. J.

Welding Transformer

Sciaky Bros, Inc., 4915 W. 67th St., Chicago, Ill., have issued Bulletin No. 328-10 covering the new Sciaky Unit Welding Transformer—a small compact



unit for portable, multiple point, package unit and indirect series welding applications.

The Sciaky transformer exceeds the minimum requirements for JIC Standards for Type 1-A transformers. This new unit offers several distinct advantages: its small, compact size—lower cost, per weld; the smaller the transformer, the more work can be done in a given area—less power loss; the smaller the transformer, the closer it can be placed to the weld.

For more complete specifications of the Sciaky Transformer, write for Bulletin No. 328-10.

Facts About Zirconium

The Carborundum Metals Co., Inc., Akron, N. Y., was organized for the dual purpose of supplying AEC and industry with zirconium metal suitable for their specific applications. At the same time, it was planned to supplement the production of metal with those zirconium chemicals for application in the chemical industry which would easily and logically fit into the operations. Work has gone along according to schedule and now—in addition to its AEC commitments, Carborundum Metals is producing zirconium metal and other related products for commercial applications.

Demand for information about zirconium has been so great that the Carborundum Metals Co., Inc., recently published a booklet "Facts About Zirconium" for general distribution.

"Facts About Zirconium" is a concise compilation of information about the history and production of zirconium. The mechanical and physical properties, chemical properties, facts about fabrication of zirconium such as melting, forging, rolling, welding and machinability are discussed. One chapter treats of zirconium chemicals such as zirconium tetrachloride, dioxide, sulfate and Carbo-Cast-Aid.

The appendix lists data on crystallographic features, atomic and nuclear properties, etc.

This factual pocket-sized booklet on zirconium is the result of collaboration between technical personnel in the Research Departments of both the Carborundum Metals Co., Inc., and the Carborundum Co.

Carbon-Steel Electrodes

Air Reduction is offering a new 12-page reprint entitled, "Carbon-Steel Electrodes for Use with Inert-Gas Shields."

Reprinted from THE WELDING JOURNAL, the booklet is a study of the properties of electrode weld deposits, for production use in the welding of carbon steel by the inert-gas-shielded metal-arc process. Pictures of weld specimens and numerous charts make this technical booklet a guide in selecting carbon-steel electrodes for a particular job.

Request a copy of this article by writing to Air Reduction Sales Co., a division of Air Reduction Co., Inc., 60 E. 42nd St., New York 17, N. Y.

Plus Cost

Accidents add to the cost of doing business. That's the theme of the National Safety Council's new booklet, "Plus Cost."

The booklet tells concisely how accidents nibble at profits and outlines the "why" and "how" of accident control.

Intended specifically for those small firms with relatively few employees and no safety staff, "Plus Cost" sets down seven easy steps for eliminating accidents and suggests sources of help.

The booklet explains in nontechnical terms just how these common-sense principles make for a good safety program and at the same time increase production.

Single copies of the 8-page booklet, attractively illustrated with two-color drawings, may be obtained by writing the Small Business Program, National Safety Council, 425 N. Michigan Ave., Chicago 11, Ill.

ALLOYS All-State FLUXES

WE'VE GOT TIME ANYTIME TO TRY TO SOLVE YOUR PROBLEM

Welding people have been tossing problems to us for years now. We like it. And, we've successfully found good answers to so many of them that today we can offer you off the shelf:

The world's most complete line of alloys and fluxes for aluminum,

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8 for stainless steel,

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Showed me how to join nichrome to copper.

Made me the world's strongest soft solder.

Gave me an electrode a beginner can use.

Showed me how to repair heat treated parts without spoiling the temper.

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PERSONNEL

Promotions

Dr. Fritz V. Lenel, Specialist in Powder Metallurgy, and Dr. Ernest F. Nippes, Jr., Director of Welding Research, have been advanced from associate to full professorships by Rensselaer Polytechnic Institute, Troy, N. Y.

Dr. Kinzel Heads Carbide Research Work

The appointment of Dr. Augustus B. Kinzel as Director of Research, Union Carbide and Carbon Corp., effective July 1st, was announced recently by Dr. George O. Curme, Jr., Vice-President—Research.

In his new capacity Dr. Kinzel will be responsible for the administration and coordination of the research activities of all Divisions of Union Carbide. These activities are carried on in seven major laboratories, operated by the various Divisions of the Corporation in South Charleston and Institute, W. Va.; Niagara Falls and Tonawanda, N. Y.; Bloomfield, N. J.; Cleveland, Ohio; and Indianapolis, Ind.

Dr. Kinzel has been actively engaged in research work with Union Carbide and Carbon Corp. since 1926, when he joined Electro Metallurgical Co. as a research metallurgist. He became chief metallurgist of the Laboratories in 1931, and a Vice-President of Electro Metallurgical Co. in 1944.

Dr. Kinzel's research contributions to the work of Union Carbide have covered a wide range of activities in the field of metallurgy, industrial gases and atomic energy. He pioneered in the theory of stainless steels, and the development of weldable alloy structural steels. He contributed to both the research and development achievements of high-vanadium high-speed steels; vanadium, chromium, and manganese metals; and extra low-carbon ferrochrome alloys. Electromet's new process for titanium crystals is largely creditable to Dr. Kinzel's research leadership. More than 40 United States patents have been issued to him.

Dr. Kinzel is the author of scores of technical and scientific articles on applied mechanics, engineering, testing, welding and metallurgy. He has been Guest Lecturer before distinguished audiences of scientists and engineers in this country, and in England, France, Germany, Italy, Belgium, Austria and Switzerland. He has given the honorary lectures of the American Institute of Mining and Metallurgical Engineers, AMERICAN WELDING SOCIETY and the American Society for Metals. He is a Samuel Wylie Miller Medalist of the AMERICAN WELDING SOCIETY, and was designated in 1953 for the Metal Progress Hall of Fame.

Dr. Kinzel was awarded the degree of Bachelor of Arts cum laude in Mathematics by Columbia University, and the Bachelor of Science in General Engineering by Massachusetts Institute of Technology. Study for his doctorate was done at the University of Nancy, in France, which awarded him the Doctors degree in Metallurgical Engineering and Science.

During recent years, Dr. Kinzel has devoted much time to many of the research and engineering problems of national defense. He headed the Government's technical intelligence section on metallurgy in Europe in 1945 with the simulated rank of Brigadier General. He is presently chairman of the Naval Research Advisory Committee, and chief consultant in metallurgy to Los Alamos Laboratories and other units of the Atomic Energy Commission. He was a member of the initial Manhattan District Committee for the World Control of Atomic Energy.

Dr. Kinzel is a vice-president of the American Institute of Mining and Metallurgical Engineers, and is active in The American Society for Metals, the American Society of Mechanical Engineers, the American Society for Testing Materials, the AMERICAN WELDING SOCIETY and the International Acetylene Assn. He is chairman of the Welding Research Council, a vice-president of the International Institute of Welding and chairman of the Advisory Committee to the Institute of Mathematical Sciences of New York University.

Dr. Kinzel's headquarters and home are in New York City.

Payne Joins Consulting Film

Payne Engineering Co., Consulting Engineers of 1812 Twenty-Eighth Ave., South Birmingham, Ala., announce that William M. Payne, B.E., Ch.E., has joined the Company as Partner and assumed duties on July 1, 1954.

Mr. Payne was formerly with the Speer Carbon Co. of St. Marys and Bradford, Pa., as Chief Engineer of the Resistor Division and as Assistant Manager of the New Products Division. More recently he was Project Manager for the John W. Danforth Co., of Buffalo, N. Y., at the Lake Ontario Ordnance Works, Model City, near Lewiston, N. Y.

Mail should be addressed to P.O. Box 5925, Birmingham 9, Ala.

McGinn Made Manager

Richard J. McGinn has been named manager of Stud Welding Co., Inc., with headquarters at 5915 Clinton Drive, Houston, Tex. The company handles distribution of Nelson stud welding equipment and products in Texas, Oklahoma, Arkansas and western Louisiana and maintains factory branch warehouses in Houston and Dallas.

McGinn, who transferred to Houston in January after three years as a field manager for the Nelson Stud Welding Division of Gregory Industries, Inc., in the Philadelphia territory, is a graduate of Bucknell University where he received a B.S. degree in Economics. A native of Ardmore, Pa., he served three years as a member of the United States Marine Corps during World War II and was engaged in sales work for Moore Business Forms, Inc., for three years in the Philadelphia area up to the time he entered the stud welding business. He is a member of the AMERICAN WELDING SOCIETY and lives at 3207 Milburn St., Houston, Tex.

S. H. Newburn Named President

Effective June 14th, S. H. Newburn was appointed President of Air Reduction Canada Ltd., it has been announced by J. A. Hill, President of Air Reduction Co., Inc.

Mr. Newburn joined Air Reduction in 1936 and since then has held various sales and management positions ranging from salesman at Cleveland to District Sales

S. H. Newburn

Manager in Detroit. In 1950, he was appointed Regional Manager of the North Central Region of Air Reduction Sales Co. with offices in Chicago, a post he has held until the present time.

Mr. Newburn will be located in Montreal, headquarters of Air Reduction Canada.

Air Reduction Canada Ltd., a division of Air Reduction Co., Inc., with sales offices in Toronto and Montreal, manufactures welding electrodes in its modern new plant in Montreal, and sells welding and cutting equipment and industrial gases to Canadian industries.

D. F. McCandlish, Chicago District Manager, succeeds Mr. Newburn as Regional Manager.

Professor Kyle Joins Lessells

Peter E. Kyle has resigned his position as Professor of Metallurgical Engineering at Cornell University, Ithaca, N. Y., to join the research and development firm of Lessells and Associates, Inc., Boston, Mass., as Vice-President. Its activities embrace the fields of mechanical engineering, electronics and mathematical physics.

Professor Kyle has been active in the work of the AMERICAN WELDING SOCIETY and the Ship Structure Committee of the Government. Professor Kyle served as Chairman of the Boston Section of the AWS and as Chairman of the Educational Committee of the AWS.

Professor Kyle was Chairman of the National Research Council Committee on Steel. This committee guided the extensive research work of the Ship Structure Committee in steel in hull construction of merchant ships.

Now—a new line-up of PAGE welding wire in coils, reels, packages

• Here are some of the ways PAGE welding wires are packaged to protect them during shipment and to make it easy for you to handle and stock them. Two new packages are Leverpaks and pallet-mounted coils.

LEVERPAKS • Here's one of the best containers for coils up to 16″ ID. It's light-weight, but durable. It is sealed air-tight and is packed with moisture-absorbing substance to reduce corrosion. Release safety catch, pull lever, and the whole top comes off. It's just as quickly resealed. Leverpaks can be rolled easily. They stack perfectly and take a minimum of floor space. They cost you nothing extra and have many re-uses.

PALLET-MOUNTED COILS • If you have a fork-lift truck, you'll like PAGE'S new pallet-mounting of 22″ ID coils. Steel strapping and paper wrapping protect coils and hold them securely. You can run them in and out of freight cars and trucks, and spot them in your plant.

AUTOMATIC WELDING WIRES • **Inert Gas**—Six PAGE-ALLEGHENY stainless grades in .035″, .045″, and .0625″ diameters. Precision thread-wound on 25-lb. non-returnable reels to fit popular arc welding machines. **Submerged Arc**—PAGE Stainless in wire diameters from 1/32″ to 5/16″, plain or copper coated. In layer-wound coils or 22″ or 24″ mill coils.

GAS WELDING RODS • In a variety of analyses: stainless, Armco, medium and high carbon, and manganese or naval bronze.

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Gruetjen Promoted

The A. O. Smith Corp., of Milwaukee, Wis., announces the appointment of F. A. Gruetjen to the staff of President L. B. Smith with the new title of Director of Aeronautical Development. He will advise A. O. Smith in all technical matters connected with the company's production of aeronautical components.

Mr. Gruetjen, who started with A. O. Smith in 1930, has been closely associated with the company's research and development activities and played a leading part in the establishment of its Air Frame and Landing Gear Divisions at Rochester, N. Y., and Toledo, Ohio, respectively.

The Propeller Division, which Mr. Gruetjen headed, will be operated under the direction of Harris Moyer, who has been with the company since 1947.

OBITUARY

C. W. Obert

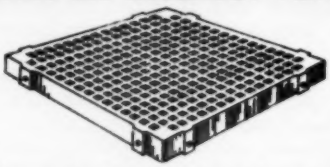
Casin W. Obert, one of the early members of the AMERICAN WELDING SOCIETY, died on April 16th in the Norwalk Hospital, Norwalk, Conn.

Casin W. Obert was awarded the Samuel Wylie Miller Memorial Medal at the 1951 Annual Meeting of the AMERICAN WELDING SOCIETY at the Hotel Book-Cadillac, Detroit, on Thursday, October 18th. This medal is awarded annually by the SOCIETY for conspicuous contributions to the advancement of welding or cutting metals. The award was made to Mr. Obert, according to the SOCIETY, for "four decades of creative contributions to the art and science of welding."

His affiliation with technical and engineering societies began in 1904 when he was elected to Associate Membership in the American Society of Mechanical Engineers. In 1912 he became Secretary of the ASME Boiler Code Committee and Assistant Secretary of the ASME, following which he was advanced to full membership in the ASME in 1914. From 1916 to 1924 he was Secretary of the American Society of Heating and Ventilating Engineers. In 1927 he was elected to membership in the AMERICAN WELDING SOCIETY. Since 1927 he has been active on numerous technical committees of the International Acetylene Assn. Mr. Obert has been Engineering Consultant for Linde Air Products Co., a Division of Union Carbide and Carbon Corp., since 1927.

Although he had curtailed much of his work at the time of his death, Mr. Obert was still an honorary member of the ASME Boiler Code Committee, a member of the New York State Boiler Advisory Board, and registered professional engineer in that State. He was also honorary member and member of the Executive Committee of the National Board of Boiler and Pressure Vessel Inspectors. In addition, he has been a member of the Editorial Committee for all three of the AWS WELDING HANDBOOKS.

WELDING and ASSEMBLY PLATENS



Floor plates — bending tables — layout tables — straightening tables — dog blocks — bending blocks — blacksmith blocks. 5′x5′ — 6′x6′ — 6′x8′ — 6′x10′ — 5′x10′ — 6′x12′ — 3′x3′ — either 6″ or 7″ thick.

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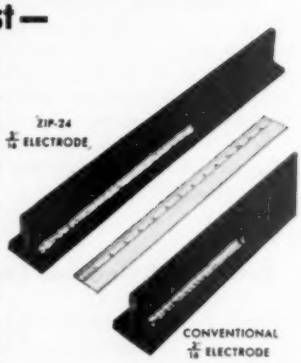
No. 4 Braz-Cast Flux for Bronze Welding Cast Iron
No. 5 & 8 Cast & Sheet Aluminum Fluxes
No. 9 Stainless Steel Welding Flux
No. 11 Tinning Compound
No. 16 Silver Solder Paste Flux

Mfg. By
ANTI-BORAX COMPOUND CO., INC.
Fort Wayne, Ind.

New Westinghouse ZIP-24 Electrode welds twice as fast— better, easier, too



FASTER WELDS—*50 to 70%* greater deposit rate. And an increase of *30 to 50%* in footage of fillet or lap weld per minute.
BETTER WELDS—balanced fillets give appearance of "machined finish" . . . no undercut with fillet and lap welds . . . perfect wall wash-up . . . easier slag removal . . . minimum spatter loss that reduces cleaning time.
EASIER WELDS — even-edged, smooth, finely rippled weld beads even when handled by an inexperienced operator. Ideal for "drag" technique.
IMMEDIATE DELIVERY—made possible with opening of our second electrode plant . . . a brand-new Westinghouse Electrode Plant at Montevallo, Alabama. To learn more about the remarkable new ZIP-24 just use coupon below.
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NEW! SIMPLIFIED! SMALLER!

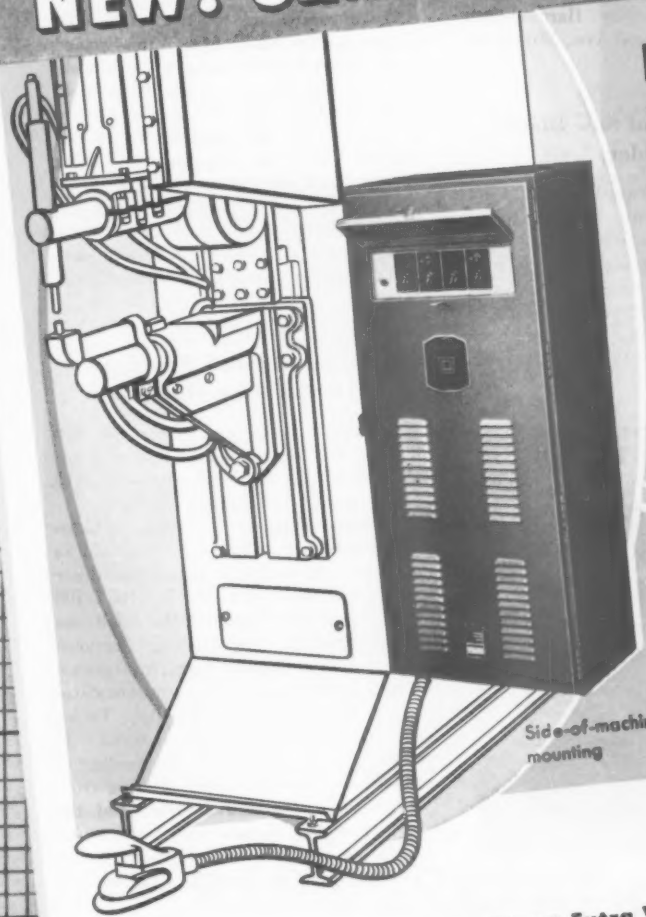
ELECTRONIC NON-SYNCHRONOUS WELDER CONTROL

Here is design simplification that affords significant advantages—easy installation, less maintenance, smaller size, mounting versatility.

Drastically simplified NEMA 3B weld and sequence timing circuit requires only 4 tubes plus valve relay and firing relay. Extreme consistency and timing accuracy are maintained. All tube circuits are "fail-safe." Firing of power tubes is synchronized to minimize transients and ease stress on welding transformer.

Interchangeable NEMA 3B and 5B timer panels are quickly detached from door. Plug-connectors to power pack in cabinet permit easy replacement.

Much smaller in size, this new controller (15" x 15" x 37") requires little more space than formerly needed for the ignitron contactor alone.



Side-of-machine mounting

Wall or catwalk mounting for gun welding applications

Wall or Machine Mounting without Extra Work

• Enclosure design permits wall or side-of-machine mounting with the same controller and without the additional trouble of relocating adjuster dial assembly. Installation is made easier by generous wiring space below contactor, and vertical wiring channel extending the full height of the cabinet. All components are arranged for maximum accessibility.

Write for Bulletin 8992 **on Electronic Non-Synchronous Welder Control and its application to spot and projection welders. Address Square D Company, 4041 N. Richards Street, Milwaukee 12, Wisconsin.**

SQUARE D COMPANY

NEW PRODUCTS

Emergency Blanket

Industrial Products Co. announces a new Emergency Blanket and Quick-Release Wall Canister for use in the event of injury, burning clothing, etc. The canister allows for immediate release of blanket; no lost time when seconds saved may determine the degree of injury or prevent possible loss of life.

Canister is of all-metal construction—$8^1/_2$ in. diam by 20 in. high—attractively finished in red enamel. Designed so that the blanket only may be removed or the entire unit lifted from the wall and taken to location of use. Blanket is 62 x 82 in., 80% wool, dark gray in color.

Full information may be obtained by writing to Industrial Products Co., 2832 N. Fourth St., Philadelphia 33, Pa.

A-C and D-C Welding Machine

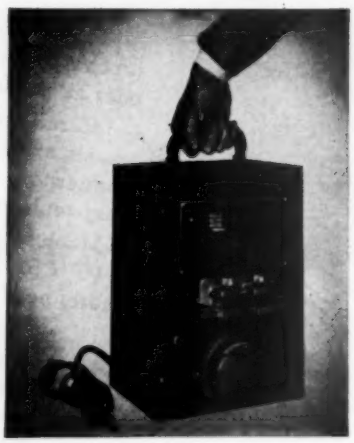
An important new development in arc welders is announced by Harnischfeger Corp., Welding Division. The P&H model DA-200 in a single machine provides both a-c and d-c welding. A flip of the switch gives the desired welding current. It is possible to change from one current to the other while welding to take advantage of the characteristics best suited for the work. This assures best possible welding results while speeding the job and cutting costs.

Now in volume production, this new P&H Welder has an a-c welding service range of from 7 to 275 amp and 7 to 200 amp d c. It operates on 220/440 v, 50/60 cycle current, single phase. It is equipped with P&H Dial-lectric control which provides instantaneous heat selection. For remote control, the radio-type dial may be taken to the work. There are no moving parts in the machine.

Complete information on this new ac-dc welder may be obtained by writing for Bulletin W-93. Address Harnischfeger Corp., 4605 W. National Ave., Milwaukee 46, Wis.

Portable Band Saw Blade Welder

The Bren/Weld Portable Band Saw Blade Welder that butt welds all blades from $^1/_{16}$ to $^3/_4$ in. has been improved with a larger, heavy-duty transformer, assuring a more uniform weld, according to Kasson Die & Motor Corp., 32-14 Northern Blvd., Long Island City 1, who are Sales Division for the manufacturer, Brennen, Bucci & Weber, Inc.



Another important improvement in this new model is the inclusion of a larger grinding unit with a motor more powerful by 50% than its predecessor. The grinder is built in, and designed to remove flash from the weld. A double gage checks thickness of weld on flat saws.

An all-purpose unit, compact and fully automatic, the Bren/Weld blade welder will handle practically all requirements of the band saw user reducing filing time tremendously as it cuts so close to the line. It permits a wide range of work from intricate internal tool and die, to power cut-off saws. Easy to use, complete instructions are printed on the name plate and simplified controls insure top performance even when handled by inexperienced people.

The Bren/Weld band saw blade welder is completely portable, weighing only 35 lb. Thus it may be carried easily from job to job enabling an entire battery of band saws to be serviced at the same time. On the other hand, permanent installation is possible if desired by removing the back plate.

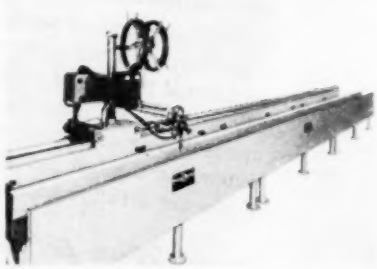
Welding jaws are constructed of solid copper and the unit is housed in a welded steel case with durable gray wrinkle finish. All-over dimensions of the Bren/Weld Portable Band Saw Blade Welder are $7^3/_4$ x 12 x 7 in. It is supplied with a convenient handle for carrying. Operating on 110 v, a c, 60 cycles, an 8-ft cord and plug are included as standard equipment.

Further information may be obtained upon request to Kasson Die & Motor Corp. 32-14 Northern Blvd., Long Island City 1, N. Y.

Automatic Rail Welding-Rebuilding Machine

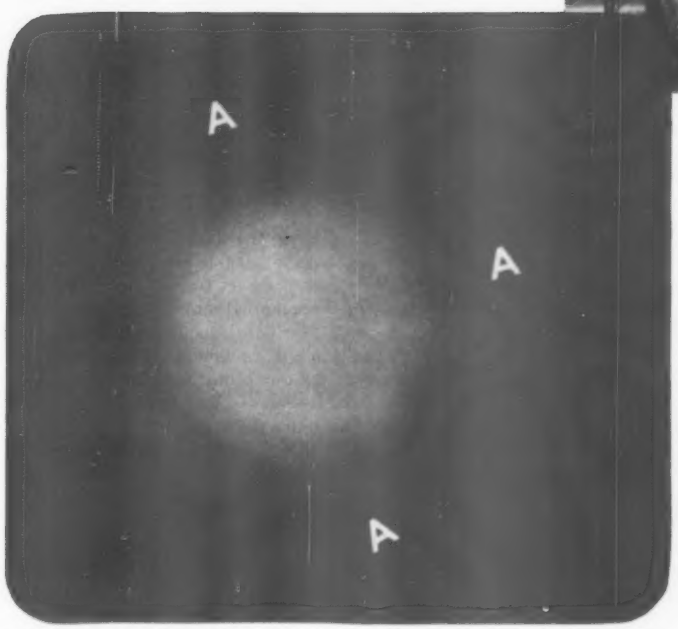
Production of a new automatic rail builder, the Mir-o-col K-10 has been announced by the Mir-o-col Alloy Co., 312 N. Avenue 21, Los Angeles 31, Calif.

Designed primarily to rebuild or hard-face extra heavy and large sections such as cat tracks, shovel pads, grader and dozer blades, the Mir-o-col K-10 can weld 1–100 in. of bead per minute. The K-10 can also be used to fabricate large sections, such as, pipe, I Beam and structural pieces. It has a 30-ft table that accommodates long lengths easily and efficiently. Tables are 32 in. wide, 7 in. deep and stand only 32 in. from the floor. The low carriage of the K-10 facilitates easier loading from either side. Distinctive features of the K-10 include a variable speed motor and adjusting wheels which permits step-overs as small as $^1/_{64}$ in. as well as a fine vertical adjustment.



Starting and stopping of the arc and flow of wire is completely automatic. Cam trips which can be preset by the operator when welding tracks automatically cut off power as head passes between links. A single lever shifts the trips so that the opposite side of the track can be rebuilt without resetting each individual cam.

Utilizing the submerged arc principle of



Radiograph of repair weld

SAVED...

66⅔% in cost, 75% in time—because

RADIOGRAPHY proved welds sound

Cracks showed up in the shell of this stainless steel sphere. A new one would cost plenty—take priceless time.

But since radiography could prove welds sound and in conformance with code, stainless patches were welded in. The sphere went back to work in one fourth the time and at one third the cost of a new replacement. This is the way radiography helps extend the use of welding and widen opportunities for welders.

Wouldn't you like to know how it can help you increase your business? Get in touch with your x-ray dealer and talk it over.

EASTMAN KODAK COMPANY
X-ray Division, Rochester 4, N. Y.

Radiography—
another important function of photography

Kodak

automatic welding, the K-10 has an automatic welding head and simplified control panel. Welding power is supplied by a 625-amp Constant Potential unit.

The automatic welding head of the K-10 is mounted on a traverse arm that can be swung in a full 360-deg circle. This added flexibility allows operator to rebuild or fabricate directly behind the table whenever it is desirable to do so.

Easily operated by one man the new Mir-o-col K-10 will handle all types and sizes of automatic wire up to $^1/_4$ in. in size.

For complete details regarding price and delivery schedules, on the K-10 automatic rebuilder, write to Mir-o-col Alloy Co., 308 N. Avenue 21, Los Angeles 31, Calif.

Welders' Helpers' Goggles

Recently announced by the Chicago Eye Shield Co. are the new Welders' Helpers' Goggles made of a new lightweight

plastic with an ultraviolet ray inhibitor The new formula plastic overcomes fogging crazing and brittleness which result when most transparent plastics are subjected to ultraviolet rays.

Designed to be worn as flash goggles by welders' helpers, or by spot welders, the CESCO No. 548-G with round rubber headband and CESCO No. 549-G with flat elastic or rubber headband, are excellent for outside use. Made in cover goggle style, they are large enough to be worn over personal glasses if necessary.

Furnished in green only, in welders and chippers styles; with 50-mm Clear Super-Safety, Anti-Glare or Cescoweld Lenses. Further information may be obtained from Chicago Eye Shield Co., 2300 W. Warren Blvd., Chicago 12, Ill.

Electrode

A new line of electrodes to weld brass, bronze, copper, cast iron and steel in all positions—flat, vertical, overhead and horizontal—has been placed on the market by Pacific Welding Alloys of Los Angeles 31, Calif.

Named "Phos-Bronze," the electrodes are a unique combination of high-grade phosphor bronze welding rods with a heavy black coating.

Because of their strength, corrosion resistance, low melting point, ease with which they can be applied and machined, the usefulness of the Phos-Bronze electrode for welding copper alloys is unequaled.

The protective coating on the rod causes the metal to pass through the arc steadily in the same manner as do conventional shielded-arc electrodes. This special coating definitely provides a softer stable arc and lower spatter.

The phosphorous content of the rod serves to keep the weld metal thoroughly deoxidized during the entire welding procedure. The high tin content gives greater strength. Absence of volatile elements reduces danger from fumes and smoke.

Pacific Phos-Bronze electrodes can be used where extensive preheating is not advisable. They can be used with direct current-reverse polarity, and work best under low amperage and high voltage. Electrodes are supplied with end grip.

A bulletin describing the new electrode in full detail, as well as samples, are available upon request to Pacific Welding Alloys Mfg. Co., 310 N. Avenue 21, Los Angeles 31, Calif.

Individuals requesting sample should specify electrode type and size, or indicate work to be done with the rod.

Electrode Holder

A molded glass fiber handle first in the industry, is one of the key features of the newly announced 14-oz. 300-amp Caddy Cub Electrode Holder. The product is manufactured by Erico Products, Inc., Cleveland, Ohio.

The use of glass fiber in the handle of this new holder achieves a long sought goal—that of a handle with high tensile strength that is also impervious to moisture, arc radiation and mechanical abuse. The material is molded with longitudinal grooves that give a firm grip and resistance to twist. At the same time the design of the handle offers reduced hand contact area for cooler operation.

This new 300-amp holder is extremely lightweight (14 oz.), only previously matched by the aluminum type holder. With the Caddy Holder the lightweight is achieved by design alone—a combination of the lightweight stamped steel body used in conjunction with the all-copper-welded Cadweld connection direct to the replaceable beryllium copper jaw. The Caddy Cub (Type B-20) operates continuous duty $^1/_{16}$–$^3/_{16}$ electrodes and $^1/_4$-in. electrodes intermittently.

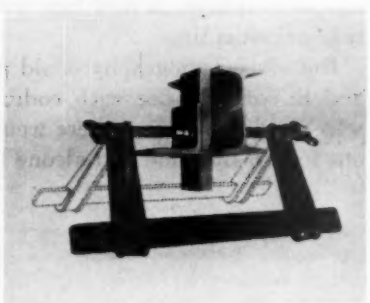
Rubber-Tired Turning Rolls

Aronson Machine Co., Arcade, N. Y., manufacturers of Positioning Equipment, announces the introduction into their line of precision turning rolls, their new line of rubber-tired turning rolls, designated at WRD and WRI models, with capacities from 5000 to 100,000 lb per unit. The most common application for these rolls is found in automatic welding of large cylindrical work. Turning speeds from 0

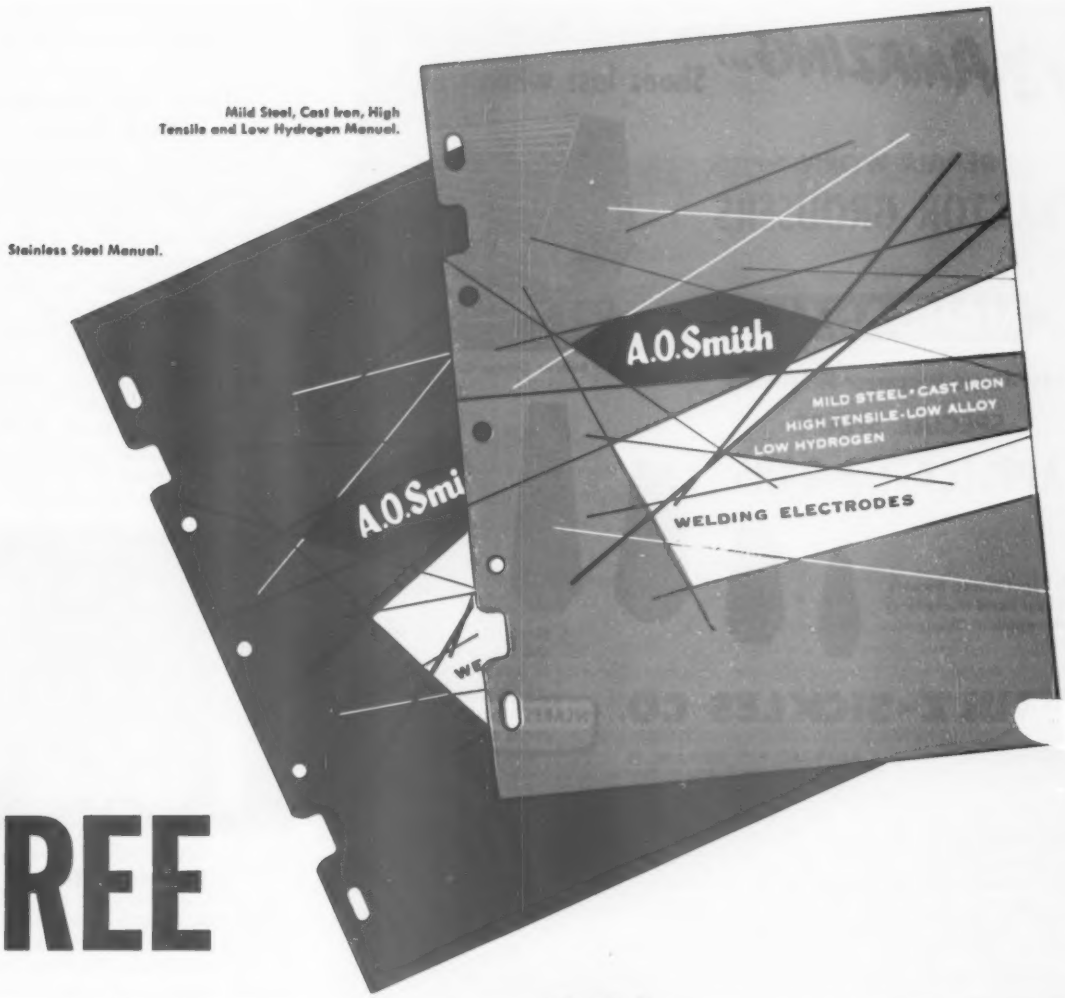
to 80 ipm in the 5000-lb class, and 0 to 38 ipm in the 100,000-lb rolls, are provided by the neat, compact transmission which has a micrometer dial control for sensitive control. The Safe Diameter Range of the Rolls is 8 in. minimum up to a maximum of 16 ft. Six center distance settings and six drive shafts are furnished to allow exactly the right amount of cradling of the work to provide steady, smooth rotation for perfect welds. The chassis is extremely rugged to allow installation on any floor or on a Rail Car, leveling at the four hold-down holes, and remaining exactly level and true regardless of loading.

Welders Clamps

The new Wetzler Double-Action Welders Clamps have been specially designed for the welding industry to facilitate the clamping of workpieces where the clamping surfaces are obstructed by overhanging portions, such as on I-bars, T-bars and various other angular sections or undercuts.



"Wetzler Double-Action Welders Clamps" rest on the same principle as "Wetzler Quick-Action Clamps," except that on the Welders Clamps both jaws slide freely on the high-carbon steel bar, while on the regular Quick-Action Clamps



FREE

...two new welding manuals

Just off the press . . . These valuable books tell in understandable language the various applications and procedures in the use of more than seventy welding electrodes covering all classes.

Complete welding information . . . Included are descriptions of weldable materials, typical mechanical properties and chemical analysis of deposited metal for each electrode, recommended welding amperages and voltages, and a quick, easy electrode selector.

In the blue, stainless manual, among many other things, you will find descriptions of problems encountered in welding stainless steels, a complete chemical analysis of all AISI stainless steels and how to weld them.

Write today for your copy . . . you can make immediate use of these books in your work. Simply mail us a postcard or letter requesting either or both of these books. Address: A. O. Smith Corporation, Welding Products Division, Milwaukee 1, Wisconsin.

IT'S AMAZING... how much longer Tractor Shoes last when you—
REPAIR WORN
TRACTOR GROUSERS
with
MANGANAL
U.S. Patents 1,876,738—1,947,167—2,021,945
11%-13½% Manganese-Nickel Steel
SPECIAL SHAPE
APPLICATOR BARS
Rebuilt Tractor Grouser
Mill Depot Stocks: Newark • Wilkes Barre (Forty-Fort) Indianapolis • Chicago
• SEND FOR LITERATURE and Handy Manganal Reference Chart
STULZ-SICKLES CO.
NEAREST DISTRIBUTOR UPON REQUEST
SOLE PRODUCERS 92 N. J. RAILROAD AVE. NEWARK, N. J.

the head jaw is always stationary. Moreover, on the Welders Clamp both jaws are provided with full length spindles permitting a 3½ in. thrust; they thus eliminate the necessity of using shims or other filling material, as is necessary with conventional clamps in order to provide a clamping surface.

Another noteworthy feature of the Welders Clamp is the facility with which the welding torch has access to any portion of the workpiece, as the clamp may be moved out of the way readily by simply rotating it about its spindle axis as shown in the illustration. For this purpose the pressure plates of the spindle turn but do not swivel.

The jaw castings are made of Grade A malleable iron. Spindles have deep cut Acme threads and are copper-plated to prevent spatter particles from adhering to them.

Full information will be promptly given by the manufacturer, the Wetzler Clamp Co., 43-15 11th St., Long Island City, N. Y.

Single Arc Stabilizer

The Mid-States Welder Manufacturing Co. of Chicago announces still further improved performance for its new Model No. 13-D "Missing Link" High-Frequency Automatic Arc Stabilizer, specified for either metallic-arc or inert-gas-shielded welding, now made adaptable to any a-c or d-c machine. The new No. 13-D Model has a universal circuit, requiring no auxiliary switch, as has been necessary on former ac-dc models.

This new circuit also produces a steady arc at low noise level, resulting in welds of highest standard at reduced operator fatigue.

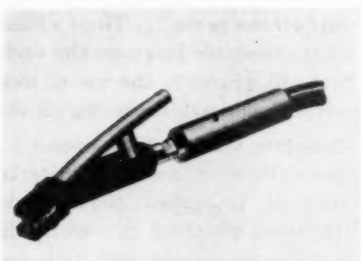
"Missing Link" No. 13-D also has a continuously variable intensity control, which permits precise adjustment for any high frequency necessary on various welding applications. These features combined with Mid-States' exclusive fully automatic gas, water, after-flow and remote control characteristics make the new "Missing Link" Model No. 13-D the most flexible and perhaps the most efficient unit in Mid-States' entire line.

Master Now Fabricates Tank Heads

At Master Tank and Welding of Dallas, Tex., tank heads of all standard specifications are now a product of their own assembly line. Fabrication of heads results in a drastic reduction in the over-all manufacturing cost of tanks and this savings will be passed on to their customers. Flanged heads from 30 in. to 20 ft diam in $^1/_2$-in. steel plate are available and smaller diameters can be furnished in lighter gages. Ellipsoidal heads not illustrated are furnished in 15, 18, 20, 24, 26 and 28 in. diam. Dome covers are 15 in. in size. Segmental hemispherical heads in 7 and 9 ft sizes.

Electrode Holder

Two new "Quick-Disconnect" welding electrode holders introduced by Cam-Lok, Division of Empire Products, Inc., Cincinnati, Ohio, are reported to completely eliminate the need for whips, whip ends or any mechanical or solder holder connections. A built-in cable connector permits the new "Quick-Disconnect" to connect to any cable end by merely adding an inexpensive Cam-Lok Female Connector.



They connect without adapters to any Cam-Lok Female Connector, according to Cam-Lok officials.

Cam-Lok's new "Quick Disconnect" electrode holders are pug nose and straight nose units. The offset jaws of the pug nose holder permit holding rods in an infinite variety of positions to make difficult-to-reach areas easily accessible. Both units feature simple, one-hand operation to make welding fast and easy.

An exclusive double cam design guarantees perfect contact and a positive, vibration-proof connection that locks tightly in only one-third of a turn. Weaving or moving of contact surfaces with resultant arcing and burning is entirely eliminated.

Completely insulated, Cam-Lok's new "Quick-Disconnect" holders have a rugged yet extremely lightweight construction.

For complete information on the new "Quick-Disconnect" welding electrode holders, write Cam-Lok Division, Empire Products, Inc., P.O. Box 98, E-59, Cincinnati 36, Ohio.

Aronson TracTred Turning Roll

Aronson Machine Co., Arcade, N. Y., manufacturers of welding positioners, are now in production of their new, improved TracTred Turning Roll. There are three basic models, T1 with single strand Trac, T2 with double strand Trac, and T3 with triple strand Trac, in both Idler and Power units. As shown in the photograph, the medium of support for cylindrical workpieces is a roller chain sling, cradling the work to provide extremely large area of contact. This feature allows equipping the Idler Roll with brass feet to conduct the weld ground current from the workpiece through the Trac and into the fixed idler chassis, eliminating the need and bother of making ground connection to each rotating cylinder. The current carrying capacity has been tested safe at 600 amp for T1, 1000 amp for T2, 1500 amp for T3. Another advantage, according to the manufacturer, lies in the ability of the TracTred to handle very thin-walled cylinders without denting or cavitation, which previously required using a supporting ring attached to the thin-walled tank for contact with the conventional wheel-type turning roll. The Power Units, T1P, T2P and T3P, are equipped with rubber feet, affording greatly increased traction, enabling the user to turn out-of-balance work smoothly enough for the most critical automatic welding. Also of real worth is the ease of adjustment and the prevention of troublesome end-creep. Rotational speed is 0 to 100 ipm, precision speed controlled to within $^{3}/_{10}$ ipm. Electrical control is by magnetic reversing starter, dust-tight oiltight remote push-button station on low-voltage circuit. Load capacities are 6000 lb per unit Model T1, 12,000 lb per unit T2, 18,000 lb per unit T3. Diameter ranges are 4–10, 6–12, 8–14 and 10–16 ft.

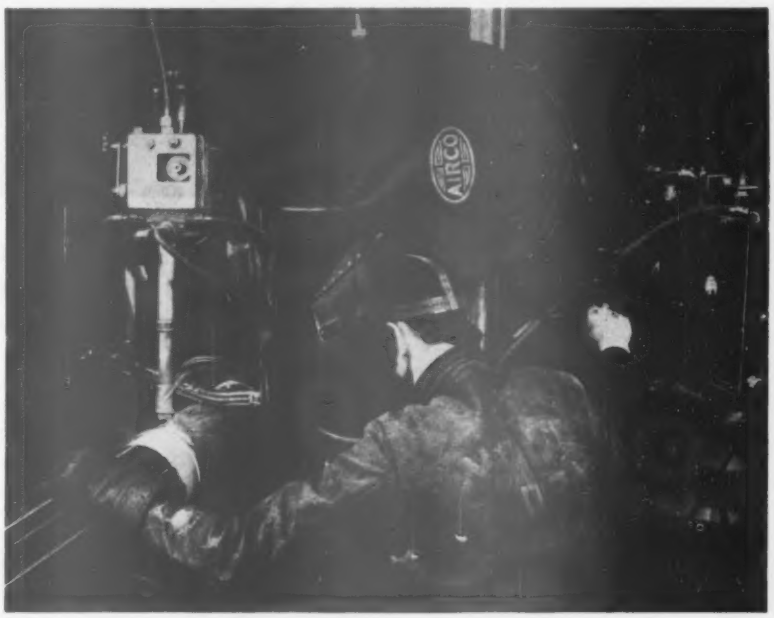
Additional information may be had by writing to Aronson Machine Co. at Arcade, N. Y.

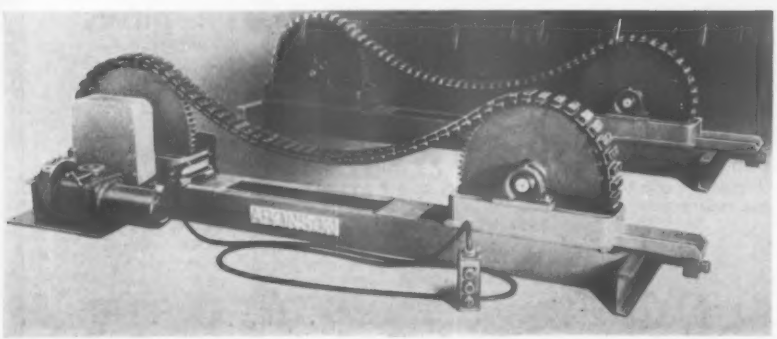
Steel Tower

An 87-ton welded steel tower, which is 9 ft wide at the base and will stand 151 ft high, is shipped from the Bethlehem, Pa., plant of Bethlehem Steel Co., for installation at the Texas Co. refinery in Westville, N. J. The hugeness of the tower, which occupies the full length of three railroad cars, is indicated by the size of the men standing alongside it.

Refrigerator Compressor Housings

Demonstrated in the Air Reduction booth at the AMERICAN-WELDING-SOCIETY sponsored Welding and Allied Industries Exposition, Buffalo, N. Y., was the automatic welding of mild steel refrigerator compressor housings. Aircomatic, Air Reduction's inert-gas-shielded metal-arc-welding process, performed at a speed of 100 ipm on the 7-in. diam compressor units.





An automatic Aircomatic Head was used in the demonstration. This unit has many exclusive advantages including a wide range of positioning adjustments for greater maneuverability, constant arc voltage, complete operating controls, more power and a greater range of drive speed. The wire feed speed range is from 100 to 450 ipm and the maximum figure can be accomplished with all kinds and diameters of wires from 0.045 in. through $^{1}/_{8}$ in.

The thickness of the low-carbon rim steel compressor housings was $^{3}/_{16}$ in. and an aluminum coated mild steel wire, $^{3}/_{32}$ in.

More than doubles welding speed

30 P&H TH-200 high-frequency machines make inert gas arc welding 2½ to 3 times faster than former gas welding at Republic Aviation Corporation

No replacement maintenance in three and a half years of two-shift operation

You don't have to take our word for the outstanding performance record of P&H Welders. The following statements are actual quotes from a certified report issued by the H. P. Gould Co. of Chicago, who made an impartial study of welding operations at Republic Aviation Corporation, Farmingdale, L. I., N. Y.

"In the building of faster and better aircraft, new developments in welding have played an important part and P&H high-frequency inert gas welding has proved that it can solve the problems of welding aluminum alloys.

"Three and a half years ago, Republic Aviation tested P&H TH-200 HF Inert Gas AC Arc Welders, and evaluated them against the existing equipment in the plant, as well as against outside competition.

"P&H welders of four different types are now used in four locations in the plant, for the welding of hundreds of different parts. In general, they are welding aluminum alloys from .032" thickness up to .250", and heat-resisting alloys from .010" up to a thickness of .093". For arc-welding alloy steels (4130, 4140, 8630, 8740, etc.), experience has proved P&H electrode AW-4 most satisfactory, and it is used exclusively.

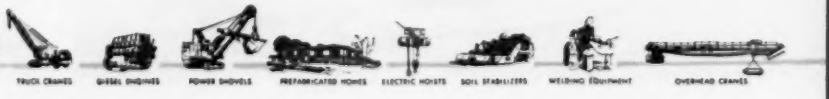
"Republic Aviation welding engineers back up the claim of the makers that P&H welders require very little maintenance. The 30 high-frequency arc welders have had no replacement maintenance whatever in three and a half years of two-shift operation — and the operating cost of each P&H TH-200 HF Welder is only 40¢ per day."

Like Republic Aviation, whose studies show P&H inert gas arc welding to be 2½ to 3 times as fast as former gas welding, you, too, can enjoy the money-saving advantages of P&H Arc Welders with Dial-lectric Control. You, too, can get faster, better welds at lower cost.

We've given you the Republic Aviation story in a nutshell. You can get all the detailed facts and figures by reading Gould Report No. 5311. Write for your copy today.

P&H WELDING DIVISION
HARNISCHFEGER CORPORATION
4551 WEST NATIONAL AVE. • MILWAUKEE 46, WIS.

the P&H Line



P&H welding equipment is manufactured and sold in Canada by
REGENT EQUIPMENT MANUFACTURING COMPANY LTD.
455 King Street West • Toronto, Ontario, Canada

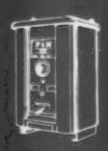
Going into Republic Aviation's "Thunderstreak" are the products and supplies of over 10,000 suppliers and subcontractors, many of whom take advantage of the same welding techniques used so effectively by Republic Aviation.

Republic Aviation meters welding current down to 5 amperes on the TH-200 HF welders. P&H's instantaneous heat control and built-in high-frequency feature permit easy regulation of high-frequency volume for individual jobs.

with P&H

Republic Aviation operates P&H high-frequency welders as standard metallic arc welders, also, so that duplicate equipment does not have to be purchased.

America's most complete line of arc-welding equipment and electrodes



P&H AC WELDER

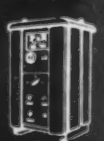
A full range of sizes up to 625 amps. (NEMA-rated). All are connectable to 220 or 440 volts—550 volts on request. Dial-lectric instantaneous Remote Control is fingertip control — gives you any heat you want — right at the work.

P&H POSITIONERS

One finger is enough to position heavy weldments for economical downhand welding. Capacities from 2500 to 36,000 lbs.— remote control and hand operated models.

THE FIRST AND ONLY AC/DC WELDER

Just a flip of a switch and you can have AC or DC welding current. This new 200-amp machine also has Dial-lectric Control for instantaneous heat selection at the work.



P&H DC RECTIFIER WELDER

Four sizes—200, 300, 400, and 500 amps. (NEMA-rated). No moving coils—no bearings, brushes, commutator, brush riggings, etc. Dial-lectric Control gives you fingertip heat-control at the work.

P&H ELECTRODES

A type and size for every job—to give you cleaner, sounder welds at lower cost on mild steel, cast iron, "hard-to-weld" steels, stainless steels, or hard surfacing.

in diameter, provided the weld metal. The shielding gas was a mixture of argon and helium plus a small percentage of oxygen.

The compressor housings are made in three sections so that internal parts can be installed. After these parts have been installed, the sections are held in a rotating fixture for welding. Two welds are required for the housing, each 21 in. in length. The 21-in. weld is completed in $12^1/_2$ sec.

LIST OF NEW MEMBERS

Effective May 31, 1954

MEMBERSHIP CLASSIFICATION

A—Sustaining Member
B—Member
C—Associate Member
D—Student Member
E—Honorary Member
F—Life Member

ALBUQUERQUE

Ball, George P. (B)
Beall, C. E. (C)
Blue, Don (C)
Boeticher, Charles W. (B)
Botts, Ernest William (C)
Carnes, Richard H. (C)
Childers, W. L. (B)
Dadian, Thomas E. (C)
Downing, Joe K. (C)
DaMant, Dan (C)
Elikins, George M., Jr. (C)
Elliott, Troy H. (C)
Felter, James R. (C)
Flick, Claude M. (B)
Flowers, J. J. (C)
Foss, Ray J. (C)
Gaddis, Charles E. (C)
Garcia, Pete V. (B)
Havo, Vern L. (C)
Howard, Owen S. (B)
Jackel, William J. (B)
Jackson, W. S. (B)
Jones, E. C. (B)
Killebrew, O. F. (B)
Kraft, L. (C)
Lee, R. G., Jr. (C)
McGee, B. H. (B)
Mora, Frank A. (B)
Nelson, Vincent (C)
Pace, Harold C. (B)
Prevost, Fred E. (B)
Radosevich, Anthony Joseph (B)
Romero, Marcos (C)
Sandoval, Lee (C)
Stanage, B. P. (C)
Stockett, W. F. (C)
Strader, John C. (C)
Sullenberger, Robert (C)
Wackernagel, Paul E. (B)
Waldorf, Herschel E. (C)
Ware, Alex Donald (C)
Whitehill, George H. (C)
Williams, Glenn E. (C)
Wilson, E. M. (C)
Woodin, S. J. (C)

BOSTON

Rogers, Robert B. (C)
Stahl, Richard C. (C)

CANADA

Clarke, Allan (C)
Marchall, Alex M. (B)

CHICAGO

Alper, Harold M. (C)
Coonrod, Forrest P. (B)
Hacker, Don J. (C)
Kleine, R. F. (B)
Pieters, Scott B. (C)
Pitchford, Charles W. (C)

CINCINNATI

Cuturilo, John (C)
Van Schwartz, Zolly C. (B)

CLEVELAND

Bennekamper, Arthur (B)
Blitch, Lynn P. (C)
George, John R. (C)
Hammond, Earl (C)
McCoy, Herb (C)
Myers, G. W. (C)
Sage, Robert E. (B)
Wilkoff, Arthur E. (B)

COLUMBUS

Sanderson, John C. (B)

DALLAS

Koenig, R. L. Bob (B)
Potrafke, William A. (C)
Williams, Charles W., Jr. (C)
Yentzen, George J. (C)

DAYTON

Mockbee, Randolph (B)
Slanker, Warren G. (C)

FOX VALLEY

Ashauer, Henry M. (C)
Barteau, Gorden (B)
Beck, Joseph J. (C)
Braatz, Rupert (C)
Brown, Le Land (C)
Cotton, Robert (C)
Danner, Eugene J. (C)
Dehn, Elmer (C)
De Noyer, Donald B. (C)
De Voe, William F. (B)
Ellifson, E. R. (C)
Engman, Edmund (C)
Franzen, Merlin (C)
Gehrt, Robert L. (C)
Hart, Edwin L. (B)
Helm, Clarence W. (B)
Huettle, Rudolph (C)
Hussner, Helm C. (C)
Jape, Donald (C)
Jezwinski, Joe (B)
Kern, Max (B)
Kotlowski, Ted (C)
Krizenesky, Thomas A. (C)
Leichtle, Hilary (C)
Leuman, Leslie H. (C)
Loos, Kenneth J. (C)
Malberg, Carl G. (B)
McDonald, Wayne D. (C)
Meyer, De Forrest (C)
Miceli, Cooper (C)
Miller, John C. (C)
Panske, Sylvester (C)
Seiler, Elmer (B)
Solchenberger, Ben (C)
Stuewer, Ewald A. (B)
Tate, Lowell (C)
Thurner, Harold W. (C)
Van Ornum, Claude (C)
Weiss, Gilbert E. (C)
Wildenberg, A. A. (B)
Wiegand, Jean (C)
Zink, Leon (C)

HARTFORD

Lawton, Richard H. (C)

HOUSTON

Bruinsma, G. (C)
Holmes, W. C. (B)
Marlow, Harry (B)
Sullivan, Thomas P. (B)
Walther, George B., Jr. (B)

KANSAS CITY

Cole, R. M. (C)

LEHIGH VALLEY

Tonay, Joseph P. (C)

LOS ANGELES

Nickliss, George E. (B)
Shuping, Jesse L. (B)

MAHONING VALLEY

Sullivan, Henry J. (B)

NEW JERSEY

Lorenzo, Larry (C)
McMoran, George (B)

NEW ORLEANS

Blackwell, W. G. (C)

NEW YORK

Brumme, Herbert (C)
Fox, Robert F. (B)
Henrich, William (B)
Johnston, George (B)
Kowalsky, William (B)
Roshdestvensky, Oleg (C)
Sacco, Edward (C)
Smart, Daniel T. (B)
Winter, Bruce B. (C)

NIAGARA FRONTIER

Dorr, Paul Eakin (B)
Harris, Tedd (B)
Mascorella, Joseph A. (B)
Simone, Robert (C)

NORTH CENTRAL OHIO

Adams, Gerald G. (C)
Augenstein, Ralph R. (C)
Bacon, Roger H. (B)
Campell, John W. (C)
Coulter, Lynn R. (C)
Deats, Glenn F. (B)
Delp, E. O. (B)
Dipert, James E. (B)
Eppley, Donald F. (C)
Finical, Harold J. (B)
Fisher, Marion L. (C)
Fitch, Floyd C. (B)
Flowers, Fred F. (B)
Focht, Morris G. (C)
Gledhill, William (B)
Griffith, Jack V. (C)
Herbert, Andrew F. (B)
Kaczmark, Raymond (B)
Kennedy, H. C. (C)
Kennedy, Robert W. (C)
Klatt, Robert (C)
Kruger, Robert (C)
Lytle, Floyd L. (C)
Mathias, Virgil J., Jr. (B)
McKay, Arthur L. (C)
Messenger, Carl E., Jr. (C)
Myers, Evan Eugene (C)
Pennisten, C. R. (C)
Philips, E. Robert (B)
Phillians, W. E. (C)
Scandling, Joseph E. (C)
Simmons, Russell E. (C)
Smith, Donald D. (C)
Smith, Howard (B)
Smith, Paul L. (B)
Snyder, Russel C. (B)
Traunero, Wm. L. (C)
Udell, Clifford E. (C)
Ward, C. L. (C)
Welch, Keith A. (B)
Workman, Leo F. (C)

NORTHEAST TENN.

Clark, F. L. (B)
Goins, M. W. (B)
Mitchell, John H. (B)

NORTHERN NEW YORK

Christoffel, Robert J. (C)
Mullen, Thomas P. (B)

OKLAHOMA CITY

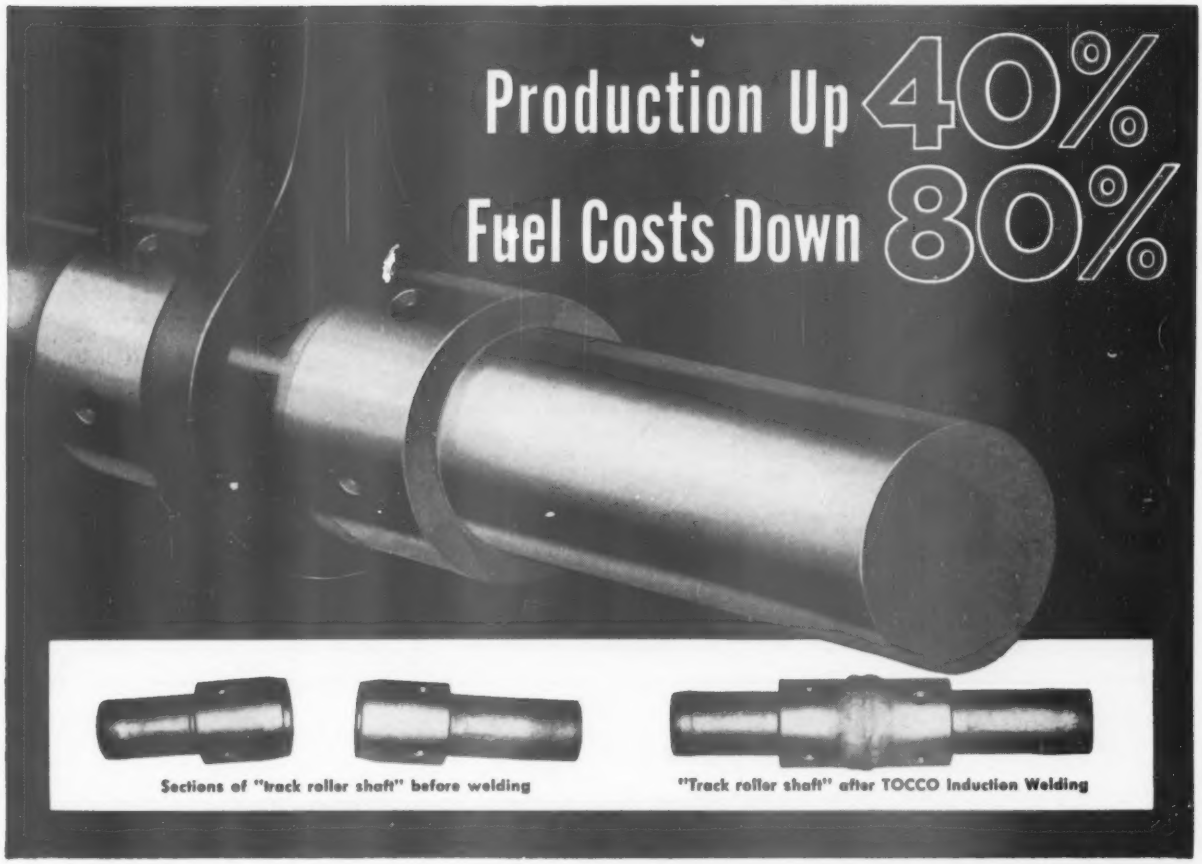
Calame, Alvin M. (D)
Johnson, Billy J. (D)
Milam, Charles R. (D)

PASCAGOULA

Hilderbrand, William C., Jr. (B)

PEORIA

Strand, Dean E. (B)


Production Up 40%
Fuel Costs Down 80%
Sections of "track roller shaft" before welding
"Track roller shaft" after TOCCO Induction Welding

with TOCCO* Induction Welding

Here's the story of how one company, a large manufacturer of automotive equipment and farm implements, is using TOCCO Induction Heating to increase production and effect substantial savings at the same time.

Production Increased

When TOCCO Induction Heating replaced oxy-acetylene, output of these hollow "track roller shafts" went up from 220 to 300 pieces per 8 hour shift. The heating cycle with TOCCO is only 55 seconds as opposed to 90 seconds formerly required with gas. Result: much lower labor cost per unit.

Fuel Savings

Fuel costs nosedived from $10.50 per hundred pieces to only $1.95—about $25 per shift saved on fuel costs alone. Other important savings have resulted from a substantial reduction in down time and maintenance costs. Quality of the weld is more uniform with Induction, and hazards of explosion present with former method are eliminated.

If the manufacture of your product involves welding, heat treating, forging, brazing or the melting of ferrous or non-ferrous metals, don't overlook TOCCO as a sound method of increasing production, improving product quality and slashing costs.

THE OHIO CRANKSHAFT COMPANY

TOCCO

Mail Coupon Today

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THE OHIO CRANKSHAFT CO.
Dept Z-8, Cleveland 1, Ohio
Please send copy of "TOCCO Induction Heating."

Name_____
Position_____
Company_____
Address_____
City_____ Zone___ State____

PHILADELPHIA

Buek, C. Harvey, (C)
Burke, Eugene F. (B)
Medlar, Lewis A. (C)
Noyes, Raymond A. (C)

PITTSBURGH

Berkey, M. Paul (C)
Eichleay, William M. (B)
Harrick, Joseph F. (B)
Lyon, George C. (C)
Schutz, Alfred (B)

RICHMOND

Butler, Northrop J. (C)
Frazier, James Andrew (B)
Gary, Hartwell H., Jr. (B)
Hendricks, William S., Jr. (C)
Mahanes, Samuel P. (C)
Michie, J. Tevis (B)
Shelton, William S. (C)

SAGINAW VALLEY

Butterfield, Robert U. (D)
Oefinger, Herbert E., Jr. (D)

SAN FRANCISCO

Laranjo, Fred F. (C)

SUSQUEHANNA VALLEY

Jadick, Nickolas (C)
McDonald, Mark (C)
Meyers, John (C)
Sartor, Lee (C)

TULSA

Kennedy, Lena (B)
McSherry, Francis (B)

WESTERN MICHIGAN

Qualls, William F. (B)

YORK-CENTRAL PA.

Vitcha, Edward T. (B)

NOT IN SECTIONS

Avigdor, Gelbetz (B)
Kirk, J. L., Jr. (C)
Masubuchi, Kouichi (C)
Rahbek, Orla Jens (B)
Riddinough, M. (B)
Sossomon, Joe B. (B)
Vaughn, John B. (B)
Yokota, Kiyoshi (B)

Members Reclassified

During the month of June

ROCHESTER

Hofherr, Fred M. (C to B)

abstracts of CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 25, D. C.

2,679,571—Tip Welder Tool—Eugene Chappel, Whitesboro, N. Y., assignor to Utica Drop Forge & Tool Corp., a corporation of New York.

Chappel's patent is on a portable tip welding tool that includes a pliers-like work gripping and holding member having electrically conductive jaws. Electrode means are provided on the tool and include a sleeve mounted for movement to and from the jaws and a reciprocable electrode mounted on the guide sleeve. Drive means are provided for advancing the electrode toward the jaws and other means urge the electrode away from the jaws. A voltage is impressed on the tool between the electrode member and the jaws.

2,680,180—High-Frequency Welding Apparatus—Donald P. Worden, Joseph M. McDonnell and Victor H. Payne, Rochester, N. Y., assignors to General Motors Corp., Detroit, Mich., a corporation of Delaware.

This welding apparatus comprises a pair of electrodes connected with a source of a-c current of relatively high frequency and which electrodes are separated by an air gap. The apparatus includes a transformer having a primary winding made from a plurality of loops of wire, each of which is on a cylindrical core of steel and which has a secondary comprising concentric spaced drums between which the primary winding is positioned. One of the drums is connected to each of the electrodes. Other means are provided for holding the article to be welded against the electrodes adjacent the air gap with a substantially constant pressure.

2,680,181—Arc Welding—Roger W. Tuthill, Fitchburg, and Alanson U. Welch, Jr., West Townsend, Mass., assignors to General Electric Co., a corporation of New York.

This patent is on a method of welding with a self-regulating arc established between a fusible electrode and the work. The method comprises energizing the welding arc from a source of supply having a rising voltage-ampere characteristic and feeding a fusible electrode to the work at a substantially constant rate of speed in accordance with its consumption in the arc for a predetermined welding current flow. The rate of metal deposition is controlled from the electrode during welding by adjusting the constant feeding rate of the electrode in order to adjust the welding current flow to maintain it within the range of desired welding current.

2,680,182—Welding Machine—William M. Chambers, Nutley, N. J., assignor to International Telephone and Telegraph Corp., a corporation of Delaware.

This machine is for welding together two parts of a workpiece with a relatively fixed welding torch in which the axis of the torch and a radial line extending from an axis of the workpiece substantially perpendicular to the plane of the contour of the welded joint to points on the same countour are substantially coplanar. This machine is adapted to make a weld the contour of which extends around the workpiece and is a specialized machine for such welding operation.

2,680,271—Cast Welding Apparatus—Donald J. Burke, Cleveland, Ohio, assignor to Erico Products, Inc., Cleveland, Ohio, a corporation of Ohio.

This apparatus relates to rail bonding and it includes a mold block having a mold cavity in its rail-engaging face adapted to receive the end of a rail bond for cast welding to such rail. Other means are provided for securing the mold block in position on a rail for the cast-welding operation.

2,680,420—Rotatable Welding Jig—John W. Sheffer, Glen Ridge, Edmund A. Watson, Ridgewood, and Arthur C. Schanz, Glen Ridge, N. J., assignors to American Car and Foundry Co., New York, N. Y., a corporation of New Jersey.

The present patent relates to a specialized rotatable jig for positioning a tacked together metal railway car during the final welding operation performed thereon.

2,680,796—Adjusting Arrangement for Electric Welding and Soldering Guns—Karl J. Aversten, Lindingo,

Sweden, assignor to Svenska Aktiebolaget Gasaccumulator, Lidingo, near Stockholm, Sweden, a corporation of Sweden.

In this instance, an adjusting arrangement for an electrical welding and soldering guns is patented and relates to a housing, means for holding the object to be welded and slidable with respect to the housing, and pivotally mounted interlocking means engageable with the holding means and slidable in the housing. Electromagnetic means are mounted in the housing and associated with the interlocking means by electromagnetic force when energized to pivot the interlocking means into interlocking engagement with the holding means and to displace the two interlocked means toward the end of the gun remote from the object to be welded.

2,680,797—Welding Apparatus—Rankin H. McDaniel, Vista, Calif., assignor to Solar Aircraft Co., San Diego, Calif., a corporation of California.

McDaniel's patent relates to apparatus for securing a plurality of studs to a base member and comprises a base, and a support mounted on the base for pivotal movement about an axis perpendicular thereto. A welding torch is mounted on the support for reciprocation along an axis parallel to the support pivot axis and means are disposed on the base beneath the welding torch for holding a plurality of studs beneath the path of movement of the torch about the base. Detent means align the torch with each stud, as desired.

2,680,798—Method of High Current Density Arc Welding—Harry I. Shrubsall, Scotch Plains, N. J., assignor to Union Carbide and Carbon Corp., a corporation of New York.

In this process, a relatively stiff arc is used and carries a relatively high current. The process comprises applying a powerful field the lines of force of which are adjacent and parallel to the arc by means of a simple conductor in the shape of a U placed on and insulated from the workpiece to form a U-loop above the arc. The legs of the arc are connected to a suitable high current source and conducting current having a value of at least half that of the welding arc is passed through such loop.

2,681,028—Welding Ring—Stanley L. Phillips, Rocky River, Ohio, assignor to Wedge Protectors, Inc., Cleveland, Ohio, a corporation of Ohio.

Phillips' patent is on a welding ring of special construction for use in securing a pair of adjacent pipe ends together.

2,681,400—Safety Device for Welders—Melvin B. Cole, Forest Hills, N. Y., assignor to Federal Electric Products Co., Newark, N. J., a corporation of Delaware.

Cole's patent relates to an automatic electric welder wherein a stationary electrode is provided and a welding cylinder is present in the apparatus and has a piston mounted therein and carrying an electrode for reciprocation towards and away from the stationary electrode. A laterally extending member is provided carrying an electrode for reciprocation towards and away from the stationary electrode. The piston has a laterally extending member thereon so that when the reciprocating electrode travels toward the stationary electrode beyond a predetermined travel distance, such laterally extending member will engage an operating member in the circuit to open a control switch for de-energizing the circuit and discontinuing the supply of welding current.

2,681,401—Arc Welding Apparatus—Nelson E. Anderson, Berkeley Heights, N. J., assignor to Air Reduction Co., Inc., a corporation of New York.

This patent relates to arc welding apparatus including an electrode holder and means for feeding filler wire to the welding zone. Positioning means are provided for the end of the filler wire and include a tubular guide through which the wire is fed to the weld zone. A pair of swivel connections are provided for positioning the tubular guide so that its position may be widely adjusted with relation to the remainder of the apparatus to vary the position of the filler wire.

2,681,402—Method of Welding Aluminum and Aluminum Alloys—Albert Muller, Plainfield, N. J., assignor to Air Reduction Co., Inc., a corporation of New York.

Muller's process comprises treating an aluminum wire to remove therefrom any hydrogen-containing compound, maintaining the aluminum wire in such hydrogen free condition and using it as a consumable electrode to arc weld by maintaining an electric arc between the end of the wire and an aluminum workpiece. The aluminum wire is continuously fed toward the workpiece and into the arc while an inert gas is delivered to the arc to form a shielding atmosphere about the arc and adjacent portions of the workpiece.

2,681,875—Method of Making an Arc Welding Composition—Leonidas Keever Stringham, Shaker Heights, and Theodore Ashton, East Cleveland, Ohio, assignors to Lincoln Electric Co., Cleveland, Ohio, a corporation of Ohio.

This patent relates to the making of a flux composition particularly suitable for use in arc welding. The method comprises heating a mixture of dry, finely ground welding flux ingredients together with a small amount of a binder and wherein the flux ingredients include silica for dry reaction with the binder. The mixture is heated to the temperature of reaction so that a silicate of a special class is formed in an incipient state of selective fusion and serves to agglomerate the other ingredients in unreacted and uniformly distributed condition in the flux composition.

2,681,968—Machine for Welding Can Opening Keys on Can Ends—Roland E. Renard, Los Gatos, Calif., assignor to Pacific Can Company, San Francisco, Calif., a corporation of Nevada.

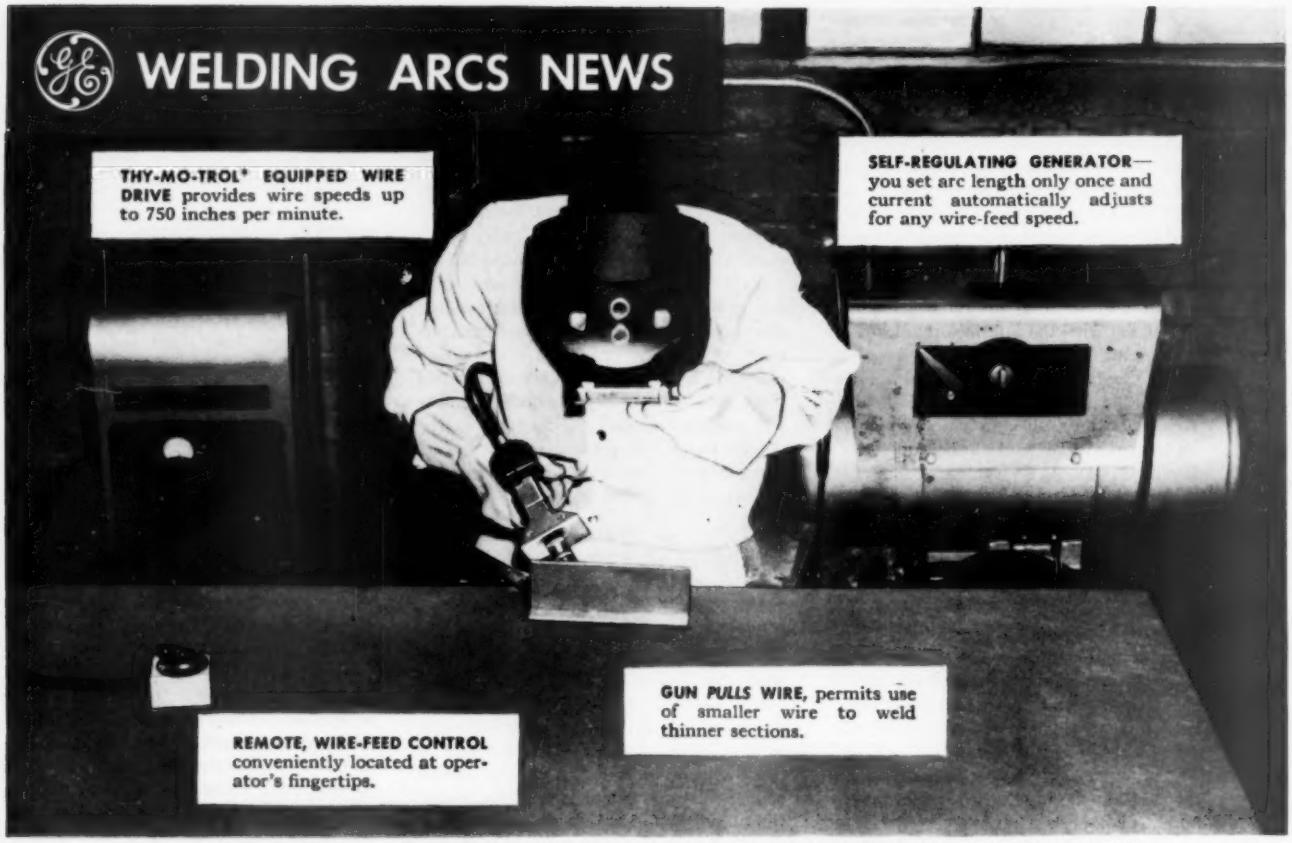
A specific type of a machine for welding keys on can ends is disclosed in and covered in such patent.

2,681,969—Welding Electrode Holder—Donald J. Burke, Cleveland Ohio, assignor to Erico Products, Inc., Cleveland, Ohio, a corporation of Ohio.

This patent relates to an electrode holder including a tubular handle of insulating material and a body member secured within the handle and projecting from one end thereof. The body member is formed to receive an electric cable which passes therethrough and connects to a detachable conductive jaw member secured to the body member. Means are provided on the body member within the handle to prevent flexing of the cable in the region of connectio. of the cable adjacent the jaw member to which the cable is secured.

2,681,970—Gas Shielded Metal Arc Welding Method—Kenneth H. Koopman, Fanwood, N. J., assignor to Union Carbide and Carbon Corp., a corporation of New York.

Koopman's patent is on a method comprising striking an arc between an end of a wire electrode being driven by a motor and being connected to a source of welding current and with the arc passing between the wire electrode and a workpiece connected to a welding current source. A weld puddle is formed on the workpiece and a stream of essentially inert gas is passed along the moving electrode to shield the arc stream while the electrode wire is fed toward the arc at a speed of the order of 100 ipm. Welding current is supplied from the source at a strength sufficient to consume the wire electrode at such rate of speed, and unenergized additional metal wire is fed from a separate source into the gas-shielded weld puddle at a rate of from 50 to 1000 ipm to increase the metal deposition rate without substantially increasing the strength of the welding current supplied to the first-mentioned electrode wire.


WELDING ARCS NEWS
THY-MO-TROL* EQUIPPED WIRE DRIVE provides wire speeds up to 750 inches per minute.
SELF-REGULATING GENERATOR—you set arc length only once and current automatically adjusts for any wire-feed speed.
GUN PULLS WIRE, permits use of smaller wire to weld thinner sections.
REMOTE, WIRE-FEED CONTROL conveniently located at operator's fingertips.

FILLERARC IS THE ONLY COMPLETE EQUIPMENT DESIGNED ESPECIALLY FOR CONSUMABLE-ELECTRODE GAS-SHIELDED WELDING

Now—demand for these Fillerarc* features permits G.E. to reduce price 15 percent

Production economies resulting from heavy customer demand, and use of expanded manufacturing facilities at General Electric's new plant at York, Pennsylvania now make possible a 15% reduction in the price of Fillerarc equipment.

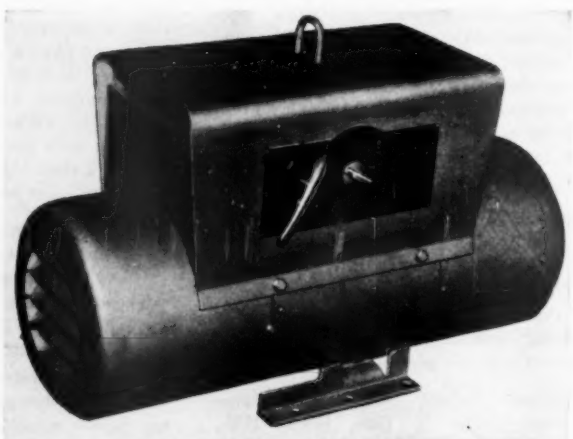
GENERATOR NOW AVAILABLE SEPARATELY

Also, you can now get the Fillerarc generator as a separate equipment. This self-regulating unit automatically supplies the right current to consume the amount of wire being fed. Once you set the arc length, current is correctly adjusted for every change in wire-feed speed.

CONTACT YOUR DISTRIBUTOR TODAY

Ask your nearest G-E welding distributor today for free bulletins on Fillerarc equipment. His name is listed on the opposite page and in the yellow pages of your phone book, under "Welding Equipment—General Electric." General Electric Co., Schenectady 5, N. Y.

*Registered trade-mark of General Electric Company.



G-E FILLERARC GENERATOR, now available as separate equipment, is the only generator with a rising volt-amp output characteristic. This minimizes burn-back, stubbing. For best performance with any consumable-electrode, gas-shielded equipment use this G-E power source.

TEST COMPARISON is made by Tractomotive Corp. employees between welds made with Strikeasy 1 and E-6012. Strikeasy 1 electrode welded 2 to 3½ times faster.

Tractomotive uses G-E Strikeasy 1 . . . increases welding speed 2 to 3½ times

General Electric powdered metal electrode is rapidly accepted by industry

Faster deposition rates . . . less operator fatigue . . . self-cleaning feature—these advantages of G.E.'s Strikeasy 1 contact electrode are being acclaimed by industry.

SAVE WITH STRIKEASY 1

The Tractomotive Corp. of Deerfield, Illinois has realized all these benefits of Strikeasy 1 in the welding of crawler tractor assemblies.

Says Tractomotive's general welding superintendent, S. E. Huffman, "Strikeasy 1 is that wonder electrode we've been needing for a long time. It's fast, practically self-cleaning and it produces a fine-quality weld. That means money saved, a boost in production, and a better all-round job."

USE FOR HIGH-SPEED JOBS

Strikeasy 1 conforms to AWS specifications for E-6012 electrodes in the flat and horizontal position.

Use Strikeasy 1 for those applications where the important factor is higher production speed. With this powdered-metal electrode, Tractomotive operators produced 21 inches of ⅜-inch fillet weld in 1½ minutes. The conventional E-6012 electrode filled the same length weld in 7½ minutes.

TEST STRIKEASY 1 ON YOUR JOB

Try Strikeasy 1 on any job where you are now using E-6012. See how it can speed up production and still produce the quality of weld you need.

For detailed information on this new standard in high-speed electrodes, see your nearest G-E welding distributor. His name and address are listed at the right.

710-18

G-E WELDING DISTRIBUTORS

Alabama: Birmingham—Alabama Oxygen, Young & Vann Supply; Mobile—Turner Supply

Arizona: Phoenix—Consolidated Welding Supply

California: Fresno, Los Angeles, Oakland, Sacramento, San Diego, San Francisco, Ventura—Victor Equipment

Colorado: Boulder, Colorado Springs, Denver, Durango, Ft. Collins, Ft. Morgan, Greeley, LaJunta, Longmont, Pueblo, Sterling—Hendrie & Bolthoff

Connecticut: Hartford, New Haven—Harris Company

Florida: Hollywood—Florida Gas & Chemical

Georgia: Atlanta, Macon—Welding Supply & Service; Augusta—Marks Oxygen; Columbus—Williams Welding Supplies

Idaho: Boise—Olson Manufacturing

Illinois: Chicago, Moline, Morton, Rockford—Machinery & Welder

Indiana: Evansville—Drillmaster Supply; Ft. Wayne, Indianapolis—Sutton-Garten; South Bend—Perry Welding Sales & Service

Iowa: Des Moines—Machinery & Welder

Kansas: Wichita—Standard Products

Kentucky: Louisville—Reliable Welding; Paducah—Henry A. Petter Supply

Louisiana: Alexandria, Shreveport—Hughes Oxygen; New Orleans—Consolidated Welding Supplies

Maryland: Baltimore—Arcway Equipment

Massachusetts: Boston—New England G-E Welding Sales Division

Michigan: Detroit—Welding Sales & Engineering; Grand Rapids—Miller Welding Supply

Minnesota: Duluth—W.P.&R.S. Mars; St. Paul—Production Materials

Mississippi: Jackson—Jackson Welding & Supply

Missouri: Kansas City—Hohenschild Welders Supply; St. Louis—Machinery & Welder

Montana: Billings—Valley Welders Supply; Billings, Bozeman, Cut Bank, Glasgow, Great Falls, Havre, Kalispell, Miles City, Shelby, Sidney, Whitefish—Valley Motor Supply; Butte, Great Falls—Montana Hardware

Nebraska: Lincoln—Lincoln Welding & Supply; Omaha—Baum Iron

New Jersey: Kenilworth—Welding Sales Corp.

New Mexico: Albuquerque—Industrial Supply Co.; Hobbs—Western Oxygen; Las Cruces, Silver City—Car Parts Depot, Inc.

New York: Buffalo—Welding Equipment Sales; New York—Welding Sales Corp.; Syracuse—Welding Engineering & Equip.

North Carolina: Charlotte—Dixie Gases; Gastonia—Gastonia Motor Parts

North Dakota: Bismarck, Fargo—Acme Welding Supply

Ohio: Akron, Cincinnati; Cleveland, Columbus, Dayton, Mansfield—Burdett Oxygen; Toledo—Odland Iron Works

Oklahoma: Oklahoma City—Hooper Supply

Oregon: Eugene, Portland—J. E. Haseltine; Medford, Portland—Industrial Air Products

Pennsylvania: Allentown, Philadelphia, Pittsburgh—Arcway Equipment

South Carolina: Columbia, Greenville—Welding Gas Products

South Dakota: Deadwood—Hendrie & Bolthoff

Tennessee: Chattanooga, Knoxville, Nashville—Welding Gas Products; Memphis—Delta Oxygen

Texas: Abilene—M&M Welding Supply; Alice, Corpus Christi—Crane Welding Supply; Alpine, El Paso, Marfa, Pecos—Car Parts Depot; Amarillo—Welding Equipment & Supply; Beaumont—H. & W. Welding Supply; Brownsville, Harlingen—Acetylene Oxygen; Dallas—Hill Equipment & Supply; Houston—G-E Welding Sales Division; Lubbock—Welders Supply of Lubbock; Midland—West Texas Welders Supply; Odessa, Pecos—Western Oxygen; Orange—Marine & Petroleum Supply; Pecos—Welding Supply Co.; Plainview—Plains Welding Supply; San Angelo—Southwestern Welding Supply; Texarkana—Hughes Oxygen; Wichita Falls—Nortex Welding Supply

Utah: Salt Lake City—The Galigher Co.

Washington: Seattle, Spokane—J. E. Haseltine; Spokane, Yakima—Industrial Air Products

West Virginia: Bluefield—Bluefield Supply; Charleston—Virginian Electric; Huntington, Logan—Logan Hardware & Supply

Wisconsin: Milwaukee—Machinery & Welder

Alaska: Anchorage—Northern Supply

Canada: Toronto—Canadian G.E.

Hawaii: Honolulu—American Factors, Ltd.

GENERAL ELECTRIC

SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Organization Meeting

Albuquerque, N. Mex.—An organization meeting of the *Albuquerque* Section was held on April 30, 1954, with Activation Chairman Willis G. Groth presiding.

The following officers were announced:

Chairman—Willis Groth
1st Vice Chairman—W. L. Childers
2nd Vice-Chairman—Charles Boetcher
Secretary—Dan Damant, Industrial Supply Co.
Chairman, Program Committee—Charles Boetcher

Chairman of various committees were also appointed.

A movie was shown. Chairman Groth explained the operation and cost of an inert-gas torch, one of which was on hand for inspection. Samples of welders were also shown in stainless steel and aluminum. The different peculiarities of this application were explained.

Wisconsin Fox River Valley

Appleton, Wis.—A new Section was recently organized in the Wisconsin Fox River Valley area to be known as the *Fox Valley* Section. Featured speaker of this organization meeting was Secretary Joe Magrath. He outlined the purposes of the group and got the new organization off to a rolling start.

With the cities of Green Bay at the north and Fond du Lac at the south, Wisconsin's Fox River Valley is roughly 100 miles of solid industry. Paper is its chief product but it also has its share of foundries, machine shops, shipyards and other manufacturing.

Welding has a hand in keeping the paper mills' huge Fourdrinier machines humming and in the general prosperity of the "Valley."

The city of Appleton, too, is the hometown of a leading manufacturer of welding equipment. This area has long felt the need for an AWS Section.

On Friday evening, May 21st, 55 welders, engineers, foremen and welding supply distributors from not only the Fox River Valley but from such points as Marinette, Sturgeon Bay, and Manitowoc met at the Elks Club in Appleton and formed what will be known in the future as the Fox Valley Section of the AMERICAN WELDING SOCIETY. Following a dinner, an election of officers was held.

William Hart of Valley Welding Supply, Appleton, was elected by the membership to serve as Chairman of the Section for the coming year. A. C. Mulder of Miller Electric Manufacturing Company, Appleton, and Joseph Jezwinski of the Berlin Chapman Co., Berlin, were made 1st and 2nd Vice-Chairmen respectively. John Wiegand, Valley Iron Works, and Carl Malmberg also of Valley Iron Works, Appleton, were elected Treasurer and Secretary of the new group and the membership selected Gordon Barteau, Appleton Structural Steel Co., Menasha, and George Vander Veldon, Thilmany Pulp and Paper Co., Kaukauna, to represent them on the Board of Directors. The Section will begin its 1954–55 program this

Fox Valley Section

Seated—left to right: John Wiegand, Treasurer; William Hart, Chairman; Carl Malmberg, Secretary. Standing—left to right: Gordon Barteau, Executive Committee: Joseph Jezwinski, Second Vice-Chairman; A. C. Mulder, First Vice-Chairman: George Vander Velden, Executive Committee

Lehigh Valley Section

Left to right—A. T. Bavaria, Lincoln Electric Co., outgoing Chairman, R. E. Somer, Bethlehem Steel Co., incoming Chairman, Fredrick L. Plummer, National President of AWS and Director of Engineering, Hammond Iron Works

fall, opening with a series of technical sessions on welding practices and developments.

Annual Ladies' Night

Bethlehem, Pa.—The annual Ladies' Night was held by the *Lehigh Valley* Section on Apr. 30, 1954, at Walp's Banquet Hall in Allentown, Pa. There was an attendance of over 100.

R. E. Sommer, incoming Chairman, introduced Mrs. Plummer, wife of the main speaker for the evening—Fredrick L. Plummer, National President of the AWS and Director of Engineering, Hammond Iron Works. Mr. Plummer gave a very interesting talk along with his colored slides on outstanding welding accomplishments such as, an all-welded building in Texas built 15 years ago, welded bridges in Europe, welded water tanks in Iceland, floating roof-type tanks in South America. Beautiful scenic colored slides were shown of Japan, South America, Panama, Mexico, and Iceland.

Following the presentation of a Past-Chairman pin to A. Bavaria, there was punch served and dancing enjoyed by all.

Plant Visit

Phoenix, Ariz.—The *Arizona* Section held its final meeting of the year on Wednesday, June 16th.

This meeting was a plant visitation and host for the evening was the Allison Steel Manufacturing Co. Following a very fine buffet style dinner and cold refreshments, a complete tour of the plant was made under the direction of A. V. McEowen [AWS].

This plant visitation was very interesting as it is one of the most complete steel manufacturing plants in the Southwest.

Election of Cincinnati Officers

Cincinnati, Ohio—The *Cincinnati* Section held its election on the 25th of May and the following officers were elected for the coming year of 1954–55:

Chairman—H. E. Schultz, Evendale, Ohio
1st Vice-Chairman—J. H. Reininger, Cincinnati, Ohio
2nd Vice-Chairman—R. M. Burthwick, Cincinnati, Ohio
Secretary-Treasurer—E. A. Harwart, Maderia, Ohio
Executive Committee: H. C. Baker, A. C. Reckman, T. L. Strouse, A. Paolina, K. R. Rouff, C. T. Whelan and G. W. Telford
Committee Chairmen: Program, J. H. Reininger; Membership, R. M. Burthwick; By-Laws, C. T. Whelan; Entertainment, G. W. Telford; Hospitality and Refreshments, A. Paolina; Special Activities, A. C. Reckman; Attendance, K. R. Rouff; Publicity, T. C. Strouse; Admission and Audit, H. C. Baker
Representatives to Technical Council: H. C. Baker; (Alternate) R. M. Burthwick; G. E. Schauerte; (Alternate) J. H. Reininger
Technical Representative to AWS: G. E. Schauerte, 216 N. "F" St., Hamilton, Ohio

Election of Officers

Davenport, Iowa.—The *Iowa-Illinois* Section elected the following officers for the 1954–55 Season:

Chairman—Frank B. Garver, Moline, Ill.
1st Vice-Chairman—J. K. McDowell, Davenport, Iowa
2nd Vice-Chairman—Ernest A. Escobar, Rock Island, Ill.
Secretary—Earl Krier, Rock Island, Ill.
Treasurer—Glen O. Fruit, Davenport, Iowa

Annual Picnic

Dayton, Ohio.—The final meeting of the *Dayton* Section was the annual picnic held at the Inland Activities Center in Dayton on June 15th. Approximately 122 members, their wives and guests were present.

The picnic began at 5 P.M. with activities available to suit all tastes and talents. Baseball, volley ball, shuffleboard and a variety of other games kept everyone busy until the dinner was ready.

After dinner, there were other games with a number of prizes being awarded to the contestants. Bob Hous, the Picnic Chairman, and his Committee are to be congratulated for the excellent planning and organization of this picnic. A good time was had by all and it is hoped that this annual affair can always be as well executed and successful.

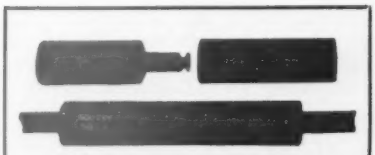
Election of Iowa Officers

Des Moines, Iowa.—On Thursday, May 20th, the *Iowa* Section held its election of officers with the following results:

Chairman—G. C. Sullivan, Solar Aircraft Co.
1st Vice-Chairman—R. D. Warde, R & R Welding Supply
2nd Vice-Chairman—T. C. Lowe, Lowe Welding Sales Co.
Secretary-Treasurer—D. E. Shapter, Solar Aircraft Co.
Chairman, Membership Committee—T. C. Lowe, Lowe Welding Sales Co.
Chairman, Program Committee—R. D. Warde, R & R Welding Supply

Quiz Program

Fort Wayne, Ind.—Members of the *Anthony Wayne* Section met on May 21st at Manochio's Grill for dinner. They adjourned to the McCoy Welding Works for a "Stump-the-Experts" demonstration.



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Tweco "Sol-Con" and "Mec-Con" plugs fit the female part of Tweco Terminal Connectors for quickly connecting cables to the welding machine and to switch polarity. They are COMPANION ITEMS.

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Nontechnical Meeting

Fort Wayne, Ind.—The June 10th meeting of the *Anthony Wayne* Section was held at the Potawatami Inn, Angola, Ind. An excellent talk on "Principle of Camp Direction" was given by George Phillips, Boys and Girls Camp Director.

Through the courtesy of the International Harvester Co., an excellent sound color film entitled "Safe Driving Practices" was shown.

Stag Nite

Grand Rapids, Mich.—The first annual Stag Nite of the *Western Michigan* Section was held on May 21st at the Brock Park Clubhouse in Ionia, Mich.

After a smörgåsbord dinner, entertainment was furnished by the Swanson Brothers.

After the excellent entertainment program, Paul Wagner presented Past-Chairman pins to Bob Kemp and John Borman and to retiring Chairman Glenn Hickok. Results of the recent election were announced and the meeting was ad-

journed. After the adjournment the Section presented the Secretary, Gordon Hill, with an aluminum golf cart. The gift was very nice and much appreciated.

Newly Elected Indiana Officers

Indianapolis, Ind.—The *Indiana* Section announces the election of the following officers for the 1954–55 season:

Chairman—James F. Salatin, Delco-Remy Division of General Motors, Anderson, Ind.
1st Vice-Chairman—Gilbert L. Foster, Allison Division of General Motors, Indianapolis, Ind.
2nd Vice-Chairman—Robert K. Waldvogel, Crosley Division of Avco Manufacturing Corp., Richmond, Ind.
Secretary—Mrs. Mercedes Spotts, P. R. Mallory Co.
Treasurer—Paul F. Grubbs, Martin Electric Co., Indianapolis, Ind.
Chairman, Membership Committee—Robert K. Waldvogel
Chairman, Program Committee—Gilbert L. Foster
Technical Represenative—C. A. Heffernon

Kansas City Officers

Kansas City, Mo.—The *Kansas City* Section announces the election of the following officers for 1954–55:

Chairman—P. D. Blake
1st Vice-Chairman—Dean Broderson
Secretary—E. R. Wycroff
Treasurer—Frank G. Singleton

Awards—Long Beach

Long Beach, Calif.—The *Long Beach* Section has undertaken a program to stimulate interest among undergraduate Junior College students in the art of welding. An Awards Committee—composed of Frank Pope, *Chairman*, Pacific Welding Supply Co.; Lee Schexschnider, Wilshire Oil Co.; Jack Carl, Shell Oil Co.; and Bill Williams, Williams Welding—was appointed to draw up plans. Thirteen Junior Colleges in the district were invited to participate.

The Committee had several meetings at which examinations, etc., were drawn up and, of course, a large amount of correspondence developed a friendly feeling of interest in the society in a heretofore untouched group. The Committee then went on a field trip and visited each of the participating schools, inspected the welding facilities, met the instructors and picked up the specimens.

Awards were made as follows: one trophy for the best arc welder; one for the best use of the oxygen-acetylene torch; and one trophy for the best combination man. The specimens submitted were surprisingly good, but the committee members were finally able to agree on the three winners. The trophies will be presented at the school exercises; not only publicizing the Society, but mainly giving a heretofore withheld recognition to the technical student.

The position of "Awards Chairman" was included as a permanent member of the Section officers this year and the trophies will be awarded annually.

Social Evening

Newark, N. J.—On Tuesday, May 18th, the *New Jersey* Section held a social evening with an informal meeting to wind up the year's activities. In lieu of the usual talk, a 45 minute film was shown. Entitled "Cerro Bolibar," it was supplied through the courtesy of the United States Steel Co.

The action involved was well filmed and showed in considerable detail the establishment of the facilities for mining, transporting to the coast and the shipping of iron ore from the newly developed deposit near the Orinoco River in Venezuela.

After the film, the officers for the coming year were introduced by Chairman Kiernan:

Chairman—Kenneth Koopman, Linde Air Products Co.
Vice-Chairman—John E. Riley, M. W. Kellogg Co.
Secretary—Leonard T. Detlor, Standard Oil Development Co.
Treasurer—Ford Pollock, Taylor Forge & Pipe Works
Past Sr. Chairman—F. O. Bodine, Midwest Piping & Supply Co.
Past Jr. Chairman—Nicholas F. Kiernan, Metal & Thermit Corp.
Members, Executive Committee: William Benz, M. W. Kellogg Co.; R. L. Deily, Air Reduction Sales Co.; E. DiLiberti, Air Reduction Sales Co.; D. Klockner, Jr., Klockner Steel Products; H. Hughey, Air Reduction Co.; T. McElrath, Linde Air Products Co.; J. Hila, Foster-Wheeler Corp.; T. B. Smith, Bethlehem Steel Co.

After the meeting refreshments were served and friendly groups mingled at length.

New Officers

New York, N. Y.—A recent election was held by the *New York* Section of new officers with the following results:

Chairman—W. E. G. Lucas, Taylor Forge & Pipe Works
1st Vice-Chairman—John Mikulak, Worthington Corp.
2nd Vice-Chairman—J. T. Stewart, Whitehead Metal Products Co.
Secretary-Treasurer—E. W. Axthelm, Linde Air Products Co.

Bridges and Buildings

Pascagoula, Miss.—On Friday evening, May 21st, a joint meeting of the *Pascagoula* Section was held in conjunction with the Mobile-Pensacola Section of the American Chemical Society, Mobile Engineers Club and Society of Military Engineers. The meeting took place at the Gulf Hunting and Fishing Club in Mobile, Ala. Through the courtesy of the Canadian Pacific Railway, an excellent film, entitled "High Powder," was shown before the call of the meeting.

Simon A. Greenberg, Technical Secretary, presented an outstanding talk on "Welding of Bridges and Buildings." Mr. Greenberg's talk was very entertaining and enjoyed by all. In all respects, this was a very successful meeting.

Annual Dinner Dance

Philadelphia, Pa.—The third annual dinner-dance was held May 15th at the Engineers Club in Philadelphia. This social night has been one of the highlights

New Jersey Section Officers
Left to right—Messers. Pollock, Koopman, Riley, Detlor

of the *Philadelphia* Section for the past few years, and certainly was no less prominent this year. Cocktails were served prior to the dinner which served not only to sharpen the appetites of those in attendance, but afforded a most opportune period for an exchange of greetings among the many members and guests, especially the fair ladies, most of whom get together but once each year.

A delicious banquet dinner was served under the soft glowing lights in the main banquent room of the Engineers Club. While partaking of this meal, a most friendly atmosphere prevailed with a background of soft dinner music.

Following the dinner, Fred Judelsohn led the singing of many old favorite (get-together) songs which were in keeping with the mood of the diners. Vocal selections were also rendered by Fred Judelsohn and Gus Garcia, and rated a grateful round of applause.

After the dinner program, dancing was enjoyed to Jo Ferena's orchestra until midnight or later. Starting with the first dance and continuing between numbers, many door prizes were awarded to the lucky ticket holders, and there were lucky tickets held. The door prizes were donated by the local business concerns in the Philadelphia area. Among the door prizes were such items as clock radios, electric toasters, electric steam irons, golf balls, picnic equipment, etc.

This social event was made so successful by the able efforts of Al Gordon, Chairman of the Dinner Program Committee. Mr. Gordon was ably assisted by Fred Judelsohn and Bob Stone for ticket sales, Dave Buerkel on dinner arrangements, D. C. Mockus and Gus Garcia on entertainment and prizes; and H. C. Heivly, who handled the program and flowers.

Annual Business Meeting

Philadelphia, Pa.—On June 8, 1954, the *Philadelphia* Section held their annual Executive Board Meeting at the Llanarch Country Club. This meeting was held for the purpose of planning the activities of the Section for the coming year, and to charge the respective committee chairmen with their duties to fulfill this program.

Bill Ostrom, Past Chairman of the Philadelphia Section, extended an invitation to the officers and board members for the afternoon and evening program to be held at the Llanarch Country Club, acting as a perfect host for the occasion. Prior to the evening business activities, Bill arranged for those members and guests who desired to tee off for a round of golf at 1:00 P.M. Needless to say, there were those who were interested and the day was perfect for the game.

After a most delicious dinner, the business meeting was called to order at 7:30 P.M. by the Section Chairman, Dave Thomas. During the course of the meeting, it was observed that the Philadelphia Section is in a very strong financial position. There were some fine new ideas advanced as to how the Section could further promote interest at Drexel Institute, also ways and means for increasing membership in the SOCIETY. A fine technical meeting program has been planned and is in the making for the coming season. The panel meetings were also discussed at length in order to come up with the very best program possible.

The other officers present were Ernie Goehringer, Vice Chairman; Gus Garcia, Secretary; Bob Irving, Recording Secretary; Tom Berg, Treasurer; Ralph Bradway, Jr., Past Chairman; and Rudi Guenzel, Past Chairman (guest); also the Executive Board—Byron Gates, Sam Baum and Whit Whitley.

The meeting adjourned at 11:00 P.M.

Slate of Officers

Rochester, N. Y.—The *Rochester* Section announces the election for 1954–55 of the following officers.

Chairman—Harvey Ainsworth
1st Vice-Chairman—Nelson Martin
Secretary—Walter Dick
Treasurer—Robert Cattanach
Chairman, Membership Committee—Richard Avery
Chairman, Program Committee—Nelson Martin
Technical Representative—Walter Dick

Saginaw Valley Officers

Saginaw, Mich.—The *Saginaw Valley* Section announces the election of the following officers for 1954–55 season:

Chairman—W. E. Moehring, Flint, Mich.
1st Vice Chairman—I. S. Makulinski, Flint, Mich.
Secretary—R. L. Woollard, Freeland, Mich.
Treasurer—J. S. Stanulis, Saginaw, Mich.
Chairman, Membership Committee—J. B. Quigley, Bay City, Mich.
Chairman, Program Committee—I. S. Makulinski, Flint, Mich.
Technical Representative—H. W. Longair, Flint, Mich.

Tri-Cities Election

Sheffield, Ala.—Election of officers for the 1954–55 season is announced by the *Tri-Cities* Section as follows:

Chairman—Fred Eckert, Sheffield, Ala.
1st Vice Chairman—Emmett Lindsay, Wilson Dam, Ala.
Secretary—Truitt Shields, Sheffield, Ala.
Treasurer—Donald B. Smith, Sheffield, Ala.
Chairman, Membership Committee—K.

One of the most satisfactory pieces of equipment for speeding up maintenance work at Sohio Petroleum Company's Latonia Refinery has been the Arcair torch. This torch has saved Sohio up to 80% in time and money where a repair job involves the gouging, cutting or removal of metal.

The maintenance department of the Latonia Refinery has been using the Arcair torch for three years . . . the seven torches in operation throughout the plant find daily use in gouging out defective welds, removing steel plates for

A worker at Sohio uses the Arcair torch to gouge out welds in the mild steel bottom plates of a 55,000-barrel floating roof tank.

vessel repair, cutting steel and alloy castings, gouging ring joint pipe flanges for alloy deposit welding and for miscellaneous cutting of metals.

At Sohio the Arcair torch has replaced the use of pneumatic chipping tools and arc rods for the majority of cutting, gouging or metal removal jobs. Before, it was a matter of chipping, which was slow, or burning, which produced undesirable slag. The maintenance department found the Arcair torch was fast and removed slag without the necessity of going over the work a second time . . . a jet of compressed air blows away the slag, leaving a clean, even surface. In most cases, further chipping or grinding is not necessary.

If gouging and cutting metals is a regular part of your plant maintenance operations ask your welding supply dealer how you can realize similar savings in time and money with the Arcair torch. He will be glad to arrange an actual demonstration in your plant. Write for free bulletin and the name of the nearest Arcair dealer in your area.

ARCAIR COMPANY
EASTERN DIVISION
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Lancaster, Ohio
WESTERN DIVISION
P. O. BOX 4227
Bremerton, Wash.

Arcair TORCH

Cuts all metals—using only electric arc and compressed air

Broutin, Southern Construction Co.
Chairman, Program Committee—Richard Ahearn, Reynolds Metals Parts Div.
Technical Representative—Richard Ahearn

Newly Elected Syracuse Officers

Syracuse, N. Y.—Election of officers by the *Syracuse* Section was held with the following results:

Chairman—Frank House
1st Vice-Chairman—Edward Strand
Secretary—Robert Larkin
Treasurer—Carl Monk
Chairman, Membership Committee—Edward Strand
Chairman, Program Committee—Arthur Ferguson, Manlius, N. Y.
Technical Representative—L. Hawthorne, Revere Copper & Brass Co., Rome, N. Y.

Tulsa Officers

Tulsa, Okla.—The following officers have been elected by the *Tulsa* Section for the 1954–55 season.

Chairman—R. H. Wainwright, Flint Steel Corp.
1st Vice-Chairman—Phil Koons, Flint Steel Corp.
2nd Vice-Chairman—L. F. Douglas, General Electric Co., Welding Div.
Secretary—Henry F. Ping, Jimmie Jones Co.
Treasurer—Vern Hartley, General Electric Co., Welding Div.
Chairman, Membership Committee—Vern Hartley
Chairman, Program Committee—Jack London, Mid-Continent Petroleum Corp.
Technical Representative—F. L. Silvers, National Tank Co.

WRC Bulletin No. 17

The Welding Research Council has just issued Bulletin No. 17 of its Bulletin Series. This Bulletin is entitled, "A Critical Survey of Brittle Failure in Carbon Plate Steel Structures Other Than Ships," by Professor M. E. Shank, Department of Mechanical Engineering, Massachusetts Institute of Technology. This report was prepared for the Committee on Ship Structural Design, of the National Academy of Sciences—National Research Council. The Committee on Ship Structural Design is advisory to the Ship Structures Committee which supported the project as part of its research program.

No similar correlation on nonship failure data exists, and this survey was therefore undertaken in order to supplement the study of ship failures.

Copies of this 48 page Bulletin may be ordered from the American Welding Society, 33 West 39th Street, New York 18, New York. Price $2.00.

INDEX TO ADVERTISERS

Air Reduction ..... Outside back cover
All-State Welding Alloys Co., Inc. 805
American Chain and Cable 807
American Manganese Steel Div. 799
Anaconda ..... Inside back cover
Anti-Borax Compound Co. 808
Arcair Company 827
Arcos Corporation 794, 795
Aronson Machine Co. 791
Dockson Corporation 802, 803
Eastman Kodak 811
Eutectic Welding Alloys Corporation 804
General Electric 822, 823
Harnischfeger Corp. 816, 817
Hobart Brothers Company 733
The International Nickel Company, Inc. 738
The Lincoln Electric Company 793
Linde Air Products Company, A Division of Union Carbide and Carbon Corp. 736
P. R. Mallory & Co., Inc. 735
Metal & Thermit Corporation 734
National Carbide Company 800
National Welding Equipment Co. 797
Ohio Crankshaft Company 819
Page Steel and Wire Division 807
Square D Company 809
Stahl Equipment Company 808
A. O. Smith 813
Stulz-Sickles Co. 814
Tweco Products Company 825
Union Carbide and Carbon Corporation Linde Air Products Company 736
Victor Equipment Company ..... Inside front cover
Westinghouse Electric Corporation 808

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Supplement to the Welding Journal, August 1954

The Welding of Heavy Sections

Preheating, peening, intermediate stress relieving, special electrodes and special techniques are some of the devices successfully used in welding very heavy sections

by W. Spraragen and M. A. Cordovi

INTRODUCTION

THE Welding Research Council was asked to prepare a critical review on the welding of heavy sections of carbon and low-alloy steels, 3 in. and up in thickness. The general idea was to develop a review of as much published literature and other information as possible on heavy weldment fabrication. Specifically, it was suggested that the data cover the following nine points: (1) joint design, (2) stress calculations and allowances, (3) types of loading—static or dynamic, (4) methods of joint preparation, (5) welding processes used, (6) welding procedures, (7) methods of inspection and tests, (8) postweld thermal treatments and (9) records of product performance.

In view of the importance and timeliness of the subject, the Council accepted the assignment. A thorough survey of the literature, however, revealed that hardly any information has been published on this subject. Therefore, a carefully prepared questionnaire was sent to key fabricators, here and abroad, known to have experience in the welding of heavy sections. Although the response to this questionnaire was disappointing, the Council nevertheless decided to release the data which has been accumulated in the belief that all could benefit from a free exchange of this information.

W. Spraragen is Director of the Welding Research Council, New York and **M. A. Cordovi,** is connected with Atomic Energy Div., Babcock & Wilcox Co., New York.

THE PROBLEM

The problem of welding heavy sections is not particularly unique and has faced the welding industry from the very beginning. The major difficulties can be divided into three broad headings, although the solution of these difficulties, in turn, has brought about other difficulties. In order of importance, the difficulties encountered in the welding of heavy sections are (1) cracking of the welds after cooling and perhaps during the fabrication procedure, (2) warpage, shrinkage and tolerances in general and (3) inconvenience to the operator.

In the early days, the practical man found solutions to these problems in preheating, peening of each layer, specially designed U joints and postheating treatments. In some instances sequence of welding was given special consideration and in recent years the development of low-hydrogen electrodes has facilitated the welding of heavy sections, although, of course, the use of these devices is not a cure-all for the problems previously mentioned.

SUMMARY*

General requirements for welding heavy sections 3 in. and over manually may be summarized as follows:

1. The preparation should be a single- or double-U or their equivalents. The width at the bottom of the U should be from $^1/_2$ in. to approximately 1 in., depending upon the depth of the groove. The included

* This Summary is based largely on information supplied by L. J. Larson, Consulting Engineer, Allis-Chalmers Manufacturing Co., Milwaukee, Wis., and F. G. Outcalt, Assistant Manager of Welding, Linde Air Products Co., New York, N. Y.

angle of the groove is specified from 10 to 12 deg for most applications. Whether to use a single- or double-U groove depends upon the thickness and the accessibility of the opposite side.

2. Electrodes of the EXX20 or EXX30 type should be used if possible. For shop welding it is generally possible to position the work so as to use downhand electrodes. However, in making repairs or for field welding, electrodes suitable for all-position welding can be used. It is probable that the EXX15 and EXX16 type electrodes may be preferable especially for welding alloy steels when preheating is difficult.

3. Preheating should be used whenever possible and the amount of preheat is determined primarily by the type of material being welded. For heavy structures a preheat of 150 to 200° F is desirable even though the material is mild steel. For the alloy steels, preheats of 300° F to as high as 500° F may be required.

4. Thorough peening of welds is desirable in heavy material. The metal should be deposited in layers about $^1/_8$ in. thick and every layer should be thoroughly peened but it is not necessary to peen after each rod on long seams. Of course the first and last layer should not be peened.

5. For welds in material over 4 in. thick, in very rigid structures of carbon steel and for even shallower welds in alloy steels, one or more intermediate stress reliefs are recommended. After the structure is completed it should be stress relieved if that is at all feasible.

For submerged arc welding several of the requirements for welding thick sections of carbon and low-alloy steels are similar to those for open arc welding. These may be summarized as follows:

1. Preparation of either single- or double-U grooves need not be quite as wide as for open arc welding. A $^3/_8$ in. radius at the bottom is ample and the included angle may be between 7 and 15 deg. The root should be about $^1/_4$ in. thick to amply support the first pass.

2. The choice of welding wire and granular welding composition will depend upon the type of steel to be welded, and the requirements of the weld.

3. The desirability of preheat and the temperature will depend upon the composition of the steel and the thickness of the joint. The high speed of submerged-arc multipass welding and high rate of heat input may make it unnecessary to continue the application of heat, or may even require cooling during welding, if the welds are relatively short and there is little time for heat dissipation between passes. The high preheat temperatures desirable for maximum improvement of certain alloy steels may be used in mechanized submerged arc welding without the limitation which consideration of operator's comfort imposes on manual welding.

4. Peening is unnecessary in submerged arc welding. Pass thicknesses are such as to make peening ineffective.

5. As with other methods of welding, intermediate stress relief treatment may be necessary to prevent root cracking of thick and rigid joints. If it is possible, however, with double-groove preparation, to deposit an inch or so thickness of weld metal alternately on each side of the joint, root cracking may be avoided without intermediate stress relieving.

SUMMARY TABLE

In order to simplify the treatment of this subject and to bring out common denominators particularly on the nine points in question, it has been decided by the reviewers to summarize the knowledge which has been made available to the Council in the form of a summary table (Table 1). For the most part the listing is chronological and the order has no significance. Following this there is given additional information on each of the cases summarized in the table including wherever possible photographs or sketches, or both. Some of these sketches have been materially simplified.

Reference 1—Steam Piping

All the details have been included in the Summary Table.

Reference 2—Valve Repair

After about two years' service handling 900 psi, 900° F steam, a 1500 psi, 14-in. nonreturn valve developed a very serious crack and required repair. The surface appearance of the crack was an irregular line 7 in. long with a maximum gap of about $^1/_{32}$ in. In the region of the crack (around the bowl of the valve) the casting was between 4 and 5 in. thick.

Preliminary chipping exploration of the crack showed it to be much more extensive than indicated by the surface crack, and it was decided to remove the crack or cracks by flame gouging and prepare a groove for repair welding. To this end the valve was fitted with induction coils and preheated 950–1050° F before any burning was done. This high preheat was used to minimize distortion since the work of cutting out and repair had to be done adjacent to accurately machined parts within the valve.

After flame gouging and grinding, the repair area extended $18^1/_2$ in. around the bowl of the valve. The repair groove varied from $2^1/_2$ to 4 in. wide and was from 4 to 5 in. deep. A representative cross section of the preparation and method of welding is shown in the sketch of Fig. 1.

After removal of the defects, the cut through to the inside of the valve was fitted with a backing bar and the large weld groove filled with beads in the conventional manner. AWS E7015 electrodes of about the same composition as the valve casting, ASTM-A217 WC3 ($^1/_2$% Cr – $^1/_2$% Mo), were used. The root layer was deposited with $^5/_{32}$ in. diam and the balance welded with electrodes $^3/_{16}$ in. diam.

During the welding operation, a minimum preheat temperature of 500° F was maintained; and, after suc-

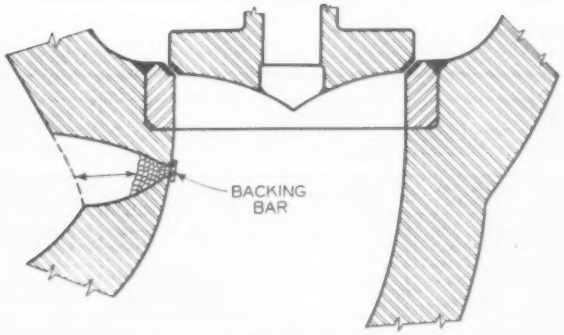


Fig. 1 Crack repair in valve

cessive layers of weld metal were deposited to a depth of $1^1/_2$ in., the temperature at the weld area was raised to 1200° F in order to effect partial stress-relief. The repair was given two short and one long stress-relief treatments when the groove was completely filled. Light peening was employed after deposition of each layer of stringer beads.

Tram marks on the casting indicated a total contraction from this repair of less than $^1/_{32}$ in., and a pressure test showed that the valve trim was still tight and had not been distorted by the weld repair.

Reference 3a—Pressure Vessels

In the early 30's several vessels of chrome-vanadium steel which had a wall thickness of $3^1/_2$ in. were built. The manufacture of these chrome-vanadium vessels presented several new problems. The steel used corresponds to an SAE 6125. The vessels were designed for operation at room temperature and at a pressure of 5000 psi. They were about 26 or 28 in. ID, with a wall thickness of $3^1/_2$ in. and had a length of about 30 ft. Since it was not possible to roll material of this thickness to such a small diameter the vessels were made of three longitudinal plates pressed to the proper curvature. This necessitated three longitudinal welds the full length of the vessel and two circular welds joining the heads to the shell section. Since there were no large openings in the vessels it was necessary to do all the welding from the outside, using backing bars at the joints. The preparation for welding consisted of machining single-U grooves with a space between the edges of the lands at the bottom of the groove so as to permit fusion on to the backing bars. Special alloy welding electrodes, which deposited metal of about the same composition as the base material, were developed. Because of the alloy base metal it was necessary to use a preheat and interpass temperature of 300° F or over. At first, considerable trouble was encountered due to both transverse and longitudinal cracks in the welds. This was overcome by extra-heavy peening and by intermediate stress relieving. After the welding was completed the vessels were stress relieved at 1100 to 1200° F and given the usual hydrostatic and hammer tests. These vessels operated satisfactorily.

Reference 3b—Plates

For building some large flywheels a number of disks 3 in. thick and 16 ft in diameter were required. At the time it was not possible to obtain plates 16 ft wide so it was necessary to weld two narrower plates together. For this purpose one 12- by 16-ft plate and one 4- by 16-ft plate were used. To allow for machining, the plates were $3^1/_2$ in. thick. It was then necessary to weld a longitudinal seam joining the 4- to the 12-ft plate with a butt joint 16 ft long. The joint preparation was a machined double-U scarf. The plates were of mild steel and they were welded with E6020 and/or E6030 electrodes. Normally such a weld should not show any trouble because of cracking. However, the welds did crack and it was not possible to eliminate the trouble by using a normal preheat and the usual amount of peening. After analyzing the problem it was concluded the difficulty was due to the bowing action of the 4-ft plate caused by heating one edge during the welding operation. To overcome this difficulty the opposite edge was heated with an oxy-acetylene torch at the same time and about the same rate as the welding heated the edge of the plate next to the groove. This method of heating kept the plate straight and no further difficulty due to cracking was encountered. The welds were inspected with X-ray and Magnaflux and also visually after machining. The welded plates were given the normal stress-relief treatment at about 1150° F. No trouble has occurred with these welds in service.

Reference 3c—Riding Rings

Cement kilns and similar equipment are generally supported on large rings called riding rings. The largest welded rings were about 14 ft OD and had a cross section of about 15 by 24 in. with the 24-in. dimension parallel to the axis of the ring. The rings were made up of two semicircular segments butt welded together. The planes of the joints were at angles of 30 deg to the axis of the ring, making machining of the welding groove rather inconvenient and expensive. For that reason the welding groove was prepared by gas cutting a narrow, double-V included angle about 10 deg, and assembled with a space of about 1 in. at the bottom of the vee. A 1-in. wide bar was tacked in place at the mid depth of the ring to form the bottom of the groove. This bar was later removed before welding on the second side in order to obtain a 100% penetration weld. For all practical purposes this preparation was the same as a double-U scarf.

The base material adjacent to the joint was preheated and kept at a temperature of 300° F or higher during the welding operation. The joint was welded manually with E6020 or E6030 electrodes. All welds were thoroughly peened and after welding was completed, the rings were stress relieved at 1100 to 1200° F. Until the betatron became available it was not possible to radiograph these joints but they were carefully checked with Magnaflux before and after machining. The service record on these rings has been satisfactory.

Table 1—Summary

SUMMARY

REFERENCE NO.	DESCRIPTION OF WELDMENT	BASE MATERIAL				JOINT		DESIGN STRESS, PSI AND TEMPERATURE, °F	WELDING DATA				PROCEDURE
		COMPOSITION	DIMENSIONS O.D. INCH	DIMENSIONS THK INCH	SPECIFIC OR GRADE	DESIGN —	PREPARATION		PROCESS	ELECTRODE TYPE AND/OR COMPOSITION	ELECTRODE DIA. INCH	REMARKS	PREHEAT, °F
1	PIPE	18Cr–8Ni Cb TO 2¼Cr – 1.0 Mo	14	3	A-213 GR. TP 347 AND T22	SINGLE U 20° GROOVE	MACH.	2100 PSI 1050 F	METAL ARC (MANUAL)	G.E. STAINLESS TYPE 1347	①	WELD MADE IN HORIZONTAL ROLLED POSITION	480–750, MAINTAINED THROUGHOUT WELDING
2	VALVE (REPAIR)	½ Cr – ½ Mo	14	4-5	A-217 GR. WC3	SINGLE V (SEE FIG. 1)	FLAME GOUGED AND GROUND	1500 PSI 900 F	METAL ARC (MANUAL)	AWS-E 7015 (½ Cr–½ Mo)	5/32 AND 3/16	LIGHT PEENING AFTER EACH PASS	500 MIN., MAINTAINED THROUGHOUT WELDING
3-(a)	PRESSURE VESSEL	Cr – V	33 TO 35	3½	SAE 6125	SINGLE U	MACH.	5000 PSI R.T.	METAL ARC (MANUAL)	Cr – V	①	ALL WELDING DONE FROM OUTSIDE, USING BACKING BARS AT JOINTS	300 MIN. MAINTAINED THROUGHOUT WELDING
3-(b)	PLATES	MILD STEEL	—	3½	①	DOUBLE U	MACH.	—	METAL ARC (MANUAL)	E 6020 AND/OR E 6030	①	WELD WAS PEENED	PREHEAT USED BUT TEMPERATURE NOT SPECIFIED
3-(c)	RIDING RINGS	C STEEL	168	15	①	DOUBLE V 10° INCL. ANGLE	GAS CUTTING	—	METAL ARC (MANUAL)	E 6020 AND/OR E 6030	①	THOROUGH PEENING	300 MIN. MAINTAINED THROUGHOUT WELDING
3-(d)	SPINDLE	Cr–Mo–V	①	3	①	SINGLE U	MACH.	—	METAL ARC (MANUAL)	E 7020 (C-Mo)	①	PEENED	500 (350 MIN. INTERPASS TEMPERATURE)
4-(a)	VANES AND RINGS	C STEEL	—	4	VANES: A-113, GR. B RINGS: A-27; GR. 65–35	SEE FIG. 2	MACH.	1000 PSI	METAL ARC (MANUAL)	E-6020	①	PEENED EACH PASS	NONE REQUIRED
4-(b)	FIELD YOKE	MILD STEEL	—	7½	①	SEE FIG. 3	LONG. WELD: FLAME CUT GIRTH WELD MACH.	—	HAND METAL ARC AND AUTOMATIC SUBMERGED METAL ARC	OXWELD 36 UNIONMELT NO. 20 FLUX, SIZE 20 x 200	1/4	PEENED EACH PASS	HAND WELDS MADE AFTER PREHEATING TO 200 F
5-(a)	AUTOCLAVE	MILD STEEL (BOILER QUALITY)	79	3⅛	LLOYDS SPEC. FOR CLASS I VESSEL	DOUBLE U (SEE FIG. 4)	MACH.	900 PSI 520 F MAX	METAL ARC (MANUAL)	LINCOLN FLEETWELD NO. 5	1/4	SEE FIG. 5	NONE REPORTED
5-(b)	BOILER DRUM	MILD STEEL (BOILER QUALITY)	①	3⅛	BRITISH STD. SPEC. NO. 1113–1952	SEE FIG. 9	MACH.	760 PSI	UNIONMELT SUBMERGED ARC AND MANUAL METAL ARC	UNIONMELT GRADE 70–20 x D	5/16	SEE FIG. 7	NONE REPORTED
6-(a)	COLUMNS	0.3C–0.15/0.30 Si	—	4	A-201 GR. A	SINGLE V AND SINGLE U	MACH. FABR.	—	SUBMERGED ARC	NO. 43 WIRE NO. 90 MELT	3/16–7/32	PEENED; BACKING STRIP NOT REMOVED	NONE REQUIRED
									AND METAL ARC	E-6020/30 W-27	¼, 5/16		
6-(b)	RAM POTS	0.3C–0.15/0.30 Si	—	6	A-212	DOUBLE U AND SINGLE U	FABR.	—	METAL ARC (ROOT PASS) UNIONMELT	E-7016 NO. 40 WIRE, NO. 80 U MELT	5/32–¼ 3/16	—	250 – 300
6-(c)	PRESS BEDS	C STEEL CASTING	—	5	—	SINGLE U, V DOUBLE V	FABR. MACH.	—	MANUAL AND SUBMERGED ARC	E 6016 AND E 6020/30 AC NO. 36 WIRE AND NO. 20 MELT	3/16	—	NONE
6-(d)	PRESS BED-REPAIR	C STEEL CASTING	—	5	—	SINGLE U	FABR.	—	METAL ARC	E 6016 AND E 6020/30 AC	3/16–7/32 ¼, 5/16	PEENED; ¼ x 1 BACKING STRIP REMOVED	250 F MAINTAINED THROUGHOUT WELDING
6-(e)	CROSS HEAD	0.3C–0.15/0.30 Si	—	3½	A-201 GR. A	SINGLE V OR DOUBLE SINGLE V; DOUBLE U	FABR.	—	MANUAL AND SUBMERGED ARC	NO. 36 WIRE, NO. 20 MELT E 6016 AND E 6020/30 AC	3/16	—	NONE
6-(f)	MILL COUPLER	1 Cr–0.2 Mo	—	6½	SAE 4140	SINGLE U	FABR.	—	MANUAL AND SUBMERGED ARC	E 9015, DC NO. 40 WIRE; NO. 80 MELT	3/16 x ¼ 3/16	—	500–600
6-(g)	GIMBAL RING	C STEEL	—	4⅜	SAE 1020	DOUBLE U	FABR.	—	METAL ARC	E 6020/30	①	BACK CHIPPED CENTER	250 F MAINTAINED THROUGHOUT WELDING
6-(h)	THROAT RING	①	—	3¼	—	SINGLE V AND SINGLE J	FABR.	—	METAL ARC	E 6020/30	①	JOINTS IN SHELL SINGLE V BACK CHIPPED	NONE
6-(i)	HALF RING	①	—	3½	—	SINGLE J	FABR.	—	METAL ARC	E 6020/30	ALL SIZES	PEENED	NONE
6-(j)	BUCKET	1.4 Mn – 0.4 Mo (MODIFIED HADFIELD)	—	①	80,000 PSI TENSILE	SINGLE V DOUBLE V	FABR	—	METAL ARC	E 10016	①	—	350
7-(a)	FLANGES	MILD STEEL	—	3¾	—	SEE FIG. 16	MACH.	—	METAL ARC	E 48 C (NFA. 81.309) BASIC COATING	15/64	PEENED	NONE REPORTED
7-(b)	HEARTH FRAME	MILD STEEL (BOILER QUALITY)	—	3⅓	—	DOUBLE V 55° INCL. ANGLE	MACH.	—	METAL ARC	E 48 C (NFA. 81.309) BASIC COATING	5/32 13/64	BACK CHIPPED; PEENED	NONE REPORTED
8-(a)	BOILER DRUM	0.45 Cr–0.45 Cu	46	3½	A.P.A.V. NO. 12	K AND DOUBLE U	MACH.	—	HAND METAL WELD (ROOT) AND UNIONMELT (MULTIPASS)	①	①	—	480 – 575
8-(b)	BOILER DRUM	1.2/1.5 Mn – 0.15 V	53	3½	A.P.A.V. NO. 12	K AND DOUBLE U	MACH.	—	HAND METAL ARC (ROOT) AND UNIONMELT (MULTIPASS)	①	①	—	480 – 575
8-(c)	BOILER DRUM	MILD STEEL	62	3⅓	50 SS	K AND DOUBLE U	MACH.	—	HAND METAL ARC (ROOT) AND UNIONMELT (MULTIPASS)	①	①	—	480 – 575
9-(a)	ROLLS	①	2/26	3½	—	DOUBLE V	MACH.	—	HAND METAL ARC, SUBMERGED ARC OR THERMIT	①	①	SUBMERGED ARC PROCESS USED ABOVE 61 IN. DIA.	NONE REPORTED
9-(b)	PLATES AND SLABS	①	2/26	3½	—	J AND U	GAS CUT AND MACH.	—	MANUAL METAL ARC, SUBMERGED ARC AND THERMIT	①	①	SUBMERGED ARC USED ABOVE 18 IN.	NONE REPORTED
10	PIPE	1 Cr – ½ Mo 2¼ Cr – 1 Mo	10/14	3⅜	SA 213 GR. GR. T22	V	MACH.	1800 PSI 1050 F	METAL ARC	A-316–48T E-9015	1/8 5/32	CHROME PLATED BACKUP RING USED	①
11	SEPARATORS	①	38	8¾	—	①	①	1500 PSI	METAL ARC	①	①	①	200
12	DIESEL SWITCHES	C STEEL	—	4½	—	BUTT & FILLET JOINTS	①	—	SUBMERGED ARC	①	1/4	OVERLAPPING FILLET WELDS	NONE
15	PRESS FRAME	C STEEL	—	3 TO 6	—	①	①	—	MANUAL AND AUTO. METAL ARC AND SUBMERGED ARC	①	①	—	300

NOTES: ① NO DETAILS

DETAILS POSTHEAT, °F	DIFFICULTIES ENCOUNTERED	SPECIAL PROCEDURES USED TO AVOID OR OVERCOME DIFFICULTIES	INSPECTION	TYPE OF APPLICATION	SERVICE PERFORMANCE RECORD	REFERENCE NO.
3 HOURS AT 1100-1200, SLOW COOLED	NONE REPORTED	NONE	GAMMA RAY AND ZYGLO	STEAM LINE ATTACHED TO A TURBINE	FOURTEEN SIMILAR WELDS IN SERVICE 3½ YEARS. SATISFACTORY PERFORMANCE	1
INTERMEDIATE AND FINAL AT 1200	NONE REPORTED	VALVE WAS FITTED WITH INDUCTION COILS AND PREHEATED TO 950-1050F TO MINIMIZE DISTORTION (DISTORTION HELD TO 1/32 INCH)	PRESSURE TEST	A 1500 PSI NONE-RETURN STEAM VALVE (CAST)	SATISFACTORY	2
1100-1200	TRANSVERSE AND LONGITUDINAL CRACKS	EXTRA HEAVY PEENING AND INTERMEDIATE STRESS-RELIEVING	HYDROSTATIC AND HAMMER TEST	STATIC LOADING (1)	SATISFACTORY	3-(a)
1150	CRACKING IN WELD	TO PREVENT BOWING CONDUCIVE CRACKING, THE EDGE OPPOSITE THE WELDING GROOVE WAS HEATED AT SAME TIME AND SAME RATE AS WELDED EDGE	X-RAY AND MAGNAFLUX	FLYWHEEL COMPONENTS	SATISFACTORY	3-(b)
1100-1200	NONE REPORTED	NONE	MAGNAFLUX	RINGS FOR CEMENT® KILNS	SATISFACTORY	3-(c)
INTERMEDIATE AND FINAL AT 1100-1200	NONE REPORTED	CRACKING PREVENTED BY MEANS OF SUITABLE PREHEAT AND USE OF CRUSHING STRIP DURING WELDING OF FIRST PASS	RADIOGRAPHY AND MAGNAFLUX	STEAM TURBINE SPINDLE	SATISFACTORY PERFORMANCE (IN SERVICE 10 YEARS)	3-(d)
1150-1200 FOR 10 HRS, FURNACE COOL	NONE REPORTED	NONE	MAGNAFLUX PERIODICALLY AND FINAL	HYDRAULIC STAY RINGS STATIC LOADING	NO UNITS HAVE BEEN PLACED IN SERVICE	4-(a)
1100 FOR 16 HRS, FURNACE COOL	SUBSURFACE LONGITUDINAL CRACKS (1/4" x 3" x 3 FT) PRESENT IN ALL HAND WELDS PROBABLY DUE TO HIGH S CONTENT OF WELD	PREHEATING TO 200 F AND PEENING EACH PASS	BETATRON	FIELD YOKE FOR 5000 HP DC MOTOR. STATIC LOADING	NO UNITS HAVE BEEN PLACED IN SERVICE	4-(b)
INTERMEDIATE AND FINAL AT 1100-1200	NONE REPORTED	HALF THE U WAS FILLED, RADIOGRAPHED AND STRESS RELIEVED AT 1100-1200F BEFORE COMPLETING THE WELD AND AGAIN STRESS RELIEVING	RADIOGRAPHY SHELL HYDRAULICALLY TESTED TO 1400 PSI	CHEMICAL EXCHANGE AUTOCLAVE	WELD INSPECTED AND FOUND TO BE SOUND AFTER 10 YEARS IN SERVICE	5-(a)
NONE REPORTED	NONE REPORTED	(1)	DRUM HYDRAULICALLY TESTED AT 1520 PSI	WATER TUBE BOILER DRUM	NOT AVAILABLE	5-(b)
1150	NONE	NONE	(1)	COLUMNS-STATIC LOADING	NOT AVAILABLE	6-(a)
1150	SLAG IN ROOT OF CIRCUMFERENTIAL JOINT	(1)	(1)	RAM POTS-	NOT AVAILABLE	6-(b)
INTERMEDIATE AND FINAL AT 1150	WELD CRACKING, WEB AND WEB TO TOP AND BOTTOM PLATE	INTERMEDIATE STRESS RELIEF OF BOTTOM PLATE AND WEB ASSEMBLY (NO DETAILS)	(1)	PRESS BEDS	NOT AVAILABLE	6-(c)
1150	NONE	NONE	(1)	PRESS BEDS	NOT AVAILABLE	6-(d)
1150	SOME OF WELDS BETWEEN WEB AND TOP AND BOTTOM PLATE CRACKED OR PULLED OUT OF PLATE	(1)	(1)	PRESS BASE	NOT AVAILABLE	6-(e)
NOT ALLOWED TO COOL PLACED IN FURNACE AT 500F AND RAISED TO 1125F; F'CE COOL	CRACKS IN WELDS WHICH HELD BRACE ACROSS JOINT FOR ASSEMBLY	GROUND OUT CRACKS	MAGNAFLUX	MAINTENANCE REPAIR, MILL COUPLER	NOT AVAILABLE	6-(f)
1150	SOME CRACKING	(1)	(1)	GIMBAL RING MAIN FRAME	NOT AVAILABLE	6-(g)
1150	SLAG IN SOME WELDS; SOME CRACKING OF HEAVY WELDS	(1)	(1)	THROAT RING HYDRO-ELECTRIC	NOT AVAILABLE	6-(h)
1150 FOR 4 HRS	DISTORTION	PEENING AND BLOCK WELDING	(1)	HEAVY HALF SHELLS FOR TEST ARRANGEMENT	NOT AVAILABLE	6-(i)
NONE	NONE REPORTED	NONE REPORTED	MAGNAFLUX	COAL STRIP MINING SHOVEL BUCKET	SATISFACTORY	6-(j)
1200	NONE REPORTED	NONE REPORTED	RADIOGRAPHY	FLANGES FOR EXHAUST PIPES IN TURBINES	SATISFACTORY GENERAL PRACTICE	7-(a)
NONE REPORTED	NONE	NONE	RADIOGRAPHY	LOW HEARTH FRAME FOR LOCOMOTIVES	SATISFACTORY GENERAL PRACTICE	7-(b)
1200, AIR COOLED	DISTORTION	TACK WELDING AND CLAMPING	RADIOGRAPHY	STEAM BOILER STATIC LOADING	SATISFACTORY GENERAL PRACTICE	8-(a)
1200, AIR COOLED	DISTORTION	TACK WELDING AND CLAMPING	RADIOGRAPHY	STEAM BOILER STATIC LOADING	SATISFACTORY GENERAL PRACTICE	8-(b)
1200, AIR COOLED	DISTORTION	TACK WELDING AND CLAMPING	RADIOGRAPHY	STEAM BOILER STATIC LOADING	SATISFACTORY GENERAL PRACTICE	8-(c)
1150	NONE REPORTED	NONE REPORTED	VISUAL	STUB ENDS TO ROLLS (REPAIR) DYNAMIC LOADING	SATISFACTORY GENERAL PRACTICE	9-(a)
1150	NONE REPORTED	NONE REPORTED	VISUAL	DYNAMIC LOADING	SATISFACTORY GENERAL PRACTICE	9-(b)
1350 COOLED AT 300F/HR	NONE	NONE	GAMMA-RAY	STEAM PIPING	NO UNITS HAVE BEEN PLACED IN SERVICE	10
1200	CRACKING DUE TO RESTRAINT	PREHEATING	VISUAL (1)	GAS/OIL SEPARATORS (FLANGE RINGS)	GENERAL	11
THERMAL STRESS RELIEVED	SEE TEXT THIS ITEM	SEE TEXT THIS ITEM	VISUAL (1)	DIESEL ELECTRIC SWITCHES	GENERAL PRACTICE	12
NONE REQUIRED	SOME CRACKING AND FRACTURES DUE TO IMPROPER DESIGN	NONE REPORTED	VISUAL	DYNAMIC LOADING	SATISFACTORY GENERAL PRACTICE	15

Reference 3d—Spindle

Because a sufficiently large forging was not available at the time, a spindle for a large steam turbine was fabricated by welding together a number of disks. Although the depth of the welds on this spindle was only 3 in. the precautions necessary to avoid trouble during welding were greater than those required on many structures having welds of greater depth. A structure consisting of heavy disks welded together in this manner is extremely rigid. The material used in this spindle was a chrome-molybdenum-vanadium steel which is quite sensitive to underbead cracking. An added problem in this case was to control the shrinkage accurately so as to keep the spindle straight and also to obtain the proper spacing between disks.

The disks were machined for a single-U weld with a backing bar. The disks were assembled with a space of about $^{5}/_{16}$ in. between the lands at the bottom of the U grooves so as to permit fusion of the weld metal to the backing bar. Manual welding with carbon-molybdenum electrodes corresponding to the present E7020 type were used.

In welding rigid structures of this type it is frequently difficult to prevent cracking of the first weld bead. To guard against this difficulty a crushing strip was used which permits relatively free shrinkage during the welding of the first pass. To prevent underbead cracking a preheat of 500° F was used with a minimum interpass temperature of 350° F. The shrinkage was controlled by peening. Careful measurements made before and during the welding operation indicated the amount of peening required. As a safety precaution the structure was given an intermediate stress relief when the welds were approximately one-half completed. After all welds were made the structure was stress relieved at 1100 to 1200° F. The welds were radiographed and carefully inspected for surface cracks with Magnaflux. No cracks or other defects in the welds were found.

The importance of preheating this type of steel was strikingly demonstrated by the underbead cracking which occurred at the fillet welds on the spacing bars used during assembly. It was not feasible to preheat the structure before these spacing bars were welded to the disks but instructions had been issued to preheat the welding areas locally before doing any welding. Evidently the amount of preheat used was insufficient and small cracks under and adjacent to the fillet welds were found when the spindle was machined. Fortunately these cracks were not of serious proportions. This spindle has been operating very satisfactorily for over ten years.

Reference 4a—Vanes and Rings

All of the available details have been included in the Summary Table. (See Fig. 2.)

Reference 4b—Field Yoke

In addition to the information given in the Summary Table, the following comments have been furnished.

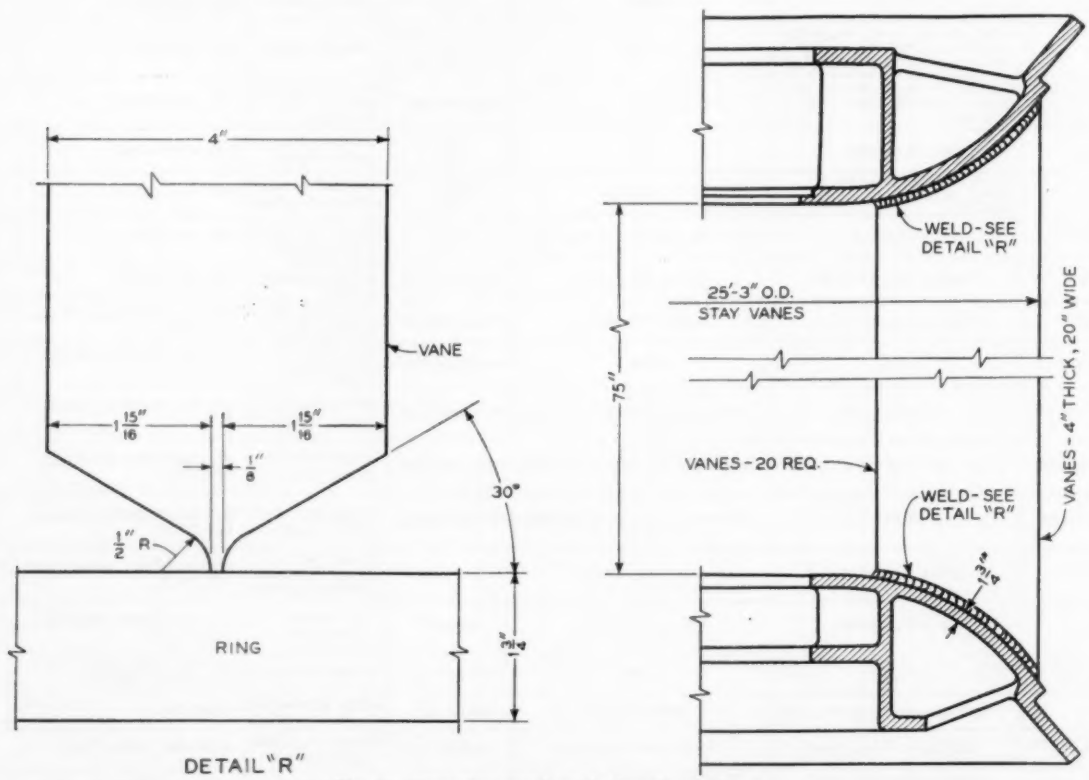


Fig. 2 Stay ring used on hydraulic turbine

Ordinarily the hand welding is not required because large enough plates are available. In three instances, the hand welds were required. Visual inspection revealed no defects. Betatron inspection showed that subsurface longitudinal cracks were present in all hand welds. The dimensions of a typical crack were: $^1/_4$ by 3 in. by 3 ft.

The reason for the cracking was not definitely established. It was believed that the high sulfur content of the weld (0.045%) may have been a responsible factor.

The defective material was removed by flame gouging after preheating to 200° F. The surfaces were ground and the grooves were repaired, after preheating to 200° F. Each pass was peened. After heat treating, the welds were given betatron inspection and were found to be satisfactory. (See Fig. 3.)

Reference 5a—Autoclave

As indicated in the Summary Table, the plate thickness was $3^1/_8$ in. (See Fig. 4.) Welding commenced from the outside until about $^1/_2$ in. of the U was filled, the landing face was then chipped out from the inside and weld metal deposited until approximately $^1/_2$ in. of the U was again filled up, making a total weld thickness deposited of about 1 in. Owing to the limited

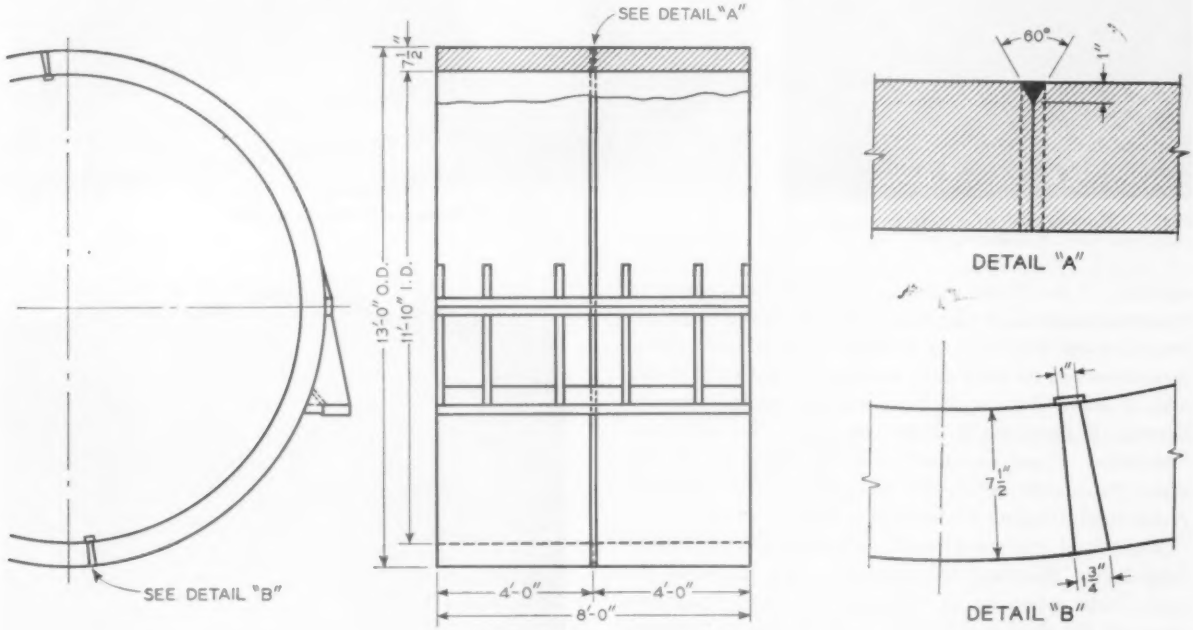


Fig. 3 Welded field yoke

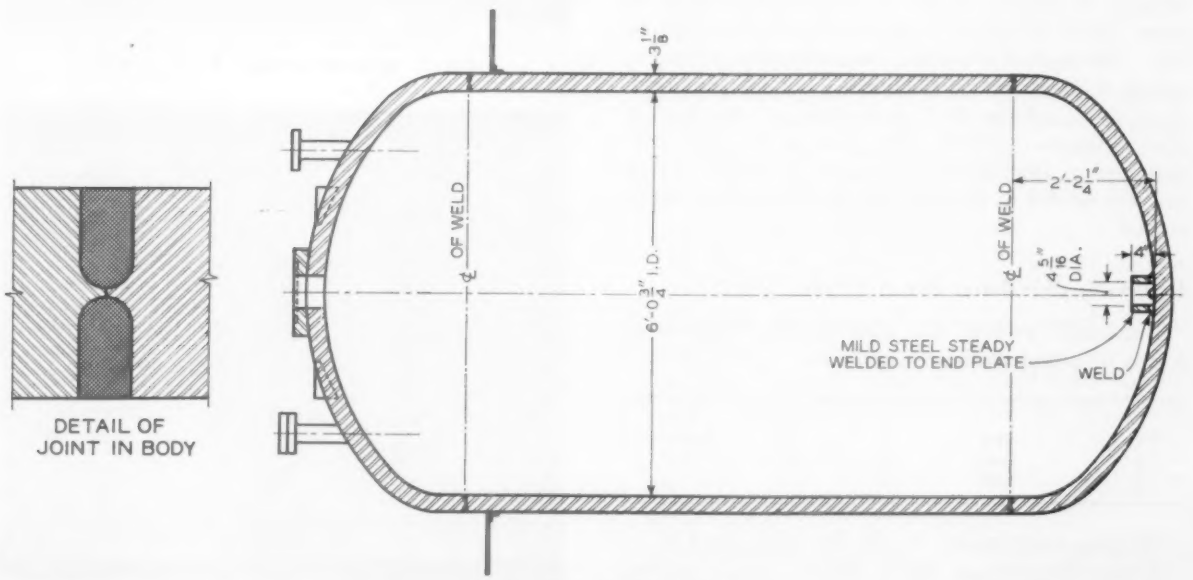


Fig. 4 Welded autoclave

Fig. 5 Macrograph of a weld in autoclave 3 1/8 in. thick

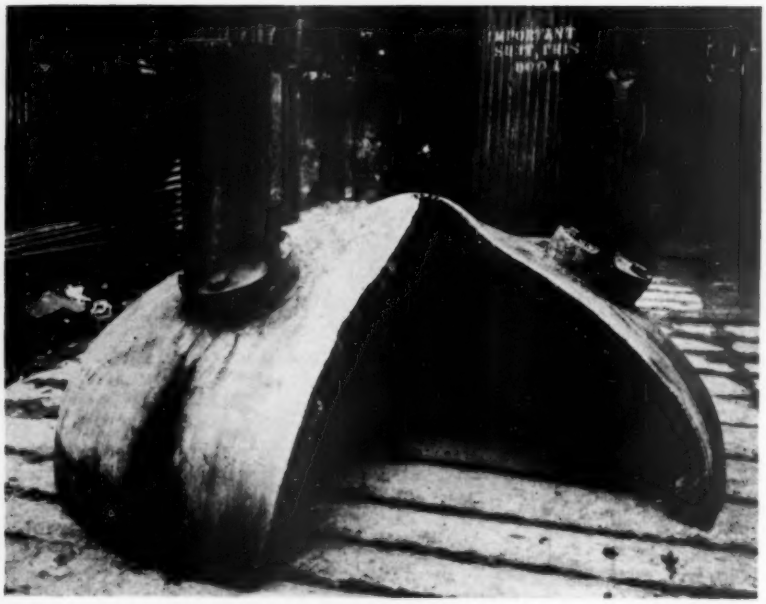


Fig. 6 Damaged autoclave head

capacity of the X-ray plant available in 1935, the weld was radiographed at this stage and thermally stress relieved at 600/650° C. A further 1/2 in. of weld metal was deposited on each side, making a total weld thickness of about 2 in. and the vessel was again stress relieved. Following this heat treatment the weld was completed, X-ray examined and the whole vessel was again stress relieved on the completion of all welding. A macrophotograph of the weld is shown in Fig. 5.

The vessel was constructed and inspected to Lloyd's Register of Shipping requirements, the whole of the main body seams being subjected to X-ray examination and the shell was hydraulically tested to 1400 psi.

The vessel was used as a chemical exchange autoclave, working at 900 psi at 270° C (maximum). After 10 years' service it was returned for a repair to the bottom end. The method of heating was coke firing and due to caking of the charge on the inside a hot spot occurred, resulting in a bulge and perforation of the bottom plate as shown in Fig. 6. The welds in the vessel were examined at this time and found to be sound, a new end was welded to the shell and the vessel returned to service.

Reference 5b—Boiler Drum 3 1/8 In. Thick

Unionmelt welding was used for the longitudinal seams, details of which are as follows:

Run	*Amp*	*V*	*Speed, in.*	*Wire diam, in.*
1	1400	35	5 1/2	5/16
2	1900	38	5 1/2	5/16

Welding composition—Grade 70. Size—20 XD.

A macrophotograph, Fig. 7, shows a typical section of the Unionmelt double run weld.

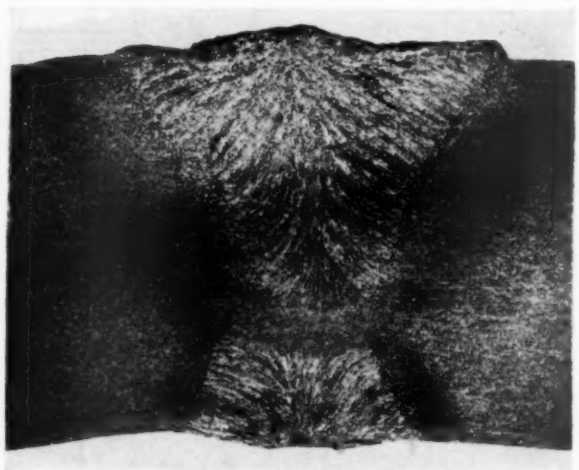


Fig. 7 Boiler drum joint 3 1/8 in. thick

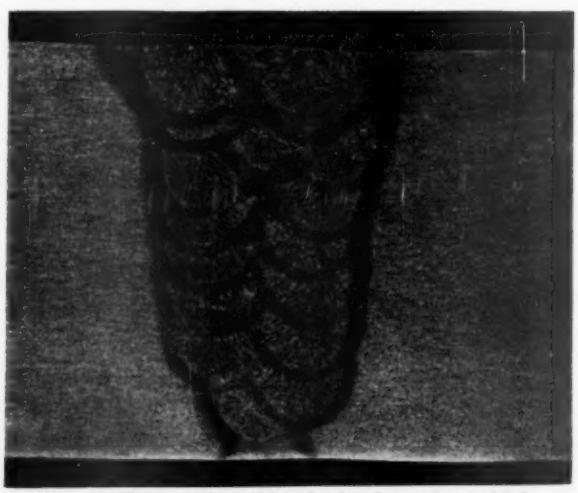
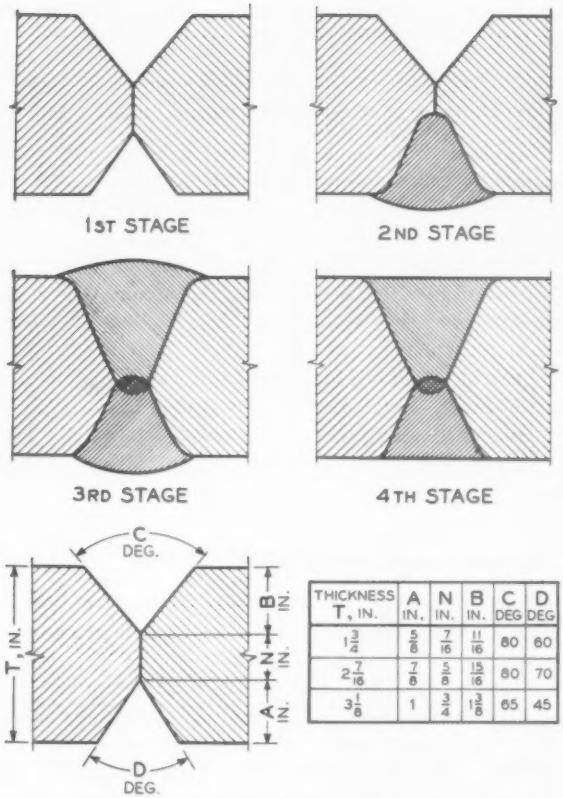


Fig. 8 Unionmelt weld in boiler drum 3 in. thick



THICKNESS T, IN.	A IN.	N IN.	B IN.	C DEG	D DEG
$1\frac{3}{4}$	$\frac{5}{8}$	$\frac{7}{16}$	$\frac{11}{16}$	80	60
$2\frac{7}{16}$	$\frac{7}{8}$	$\frac{5}{8}$	$\frac{15}{16}$	80	70
$3\frac{1}{8}$	1	$\frac{3}{4}$	$1\frac{3}{8}$	65	45

Fig. 9 Welding details for boiler drum meeting British standard requirements

The circumferential seams may be Unionmelt multirun or manual metallic-arc welded. A typical section of a Unionmelt multirun weld in 3-in. plate is shown in Fig. 8.

The whole of the main seams were X-ray examined and the completed drum subjected to hydraulic test in accordance with the requirements of British Standards Specification 1113-1952. (See Fig. 9.)

Reference 6a—Columns

These columns are of great length and weight, and are to be used in a large aluminum extruded section stretcher. They are 138 ft long with a base of 5 ft 2 in. by 3 ft $10\frac{1}{2}$ in. Cross section is given in Fig. 10.

Material Specification: ASTM SA201, Grade A. 2- and 4-in. gage.

Type of Weld Kerf: Corner joint single V for submerged arc. All inside welds were $\frac{3}{4}$-in. fillets. Butt joint between sections consisted of fabricated single U. Backing strip not removed.

Weld Metal, Specifications and Size of Electrode: Butts made with AWS E6020-30. W-27 $\frac{3}{16}$–$\frac{7}{32}$, $\frac{1}{4}$ and $\frac{5}{16}$ diam. Current, AC. Weld sequence used to balance joint, and residual compression stress in welds which are subject to partial tension when in operation. Peened for relief of stress and minimum distortion.

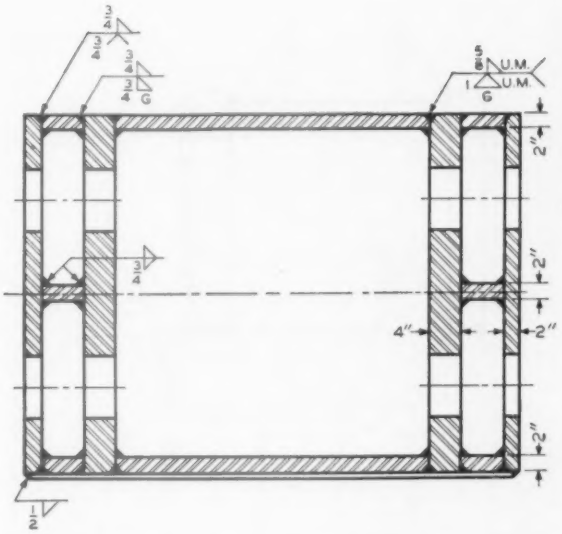


Fig. 10 Cross section of heavy columns for stretcher

Preheat: None—except once welding was started, heat of welding maintained in joints by gas burner as welding sequence was followed.

Postheat: None—four sections were stress relieved at 1150° F and held for 1 hr per inch of thickness. Furnace cooled.

Difficulties Encountered: None.

Degree of Restraint: Width and length restrained because of thickness.

Reference 6b—Ram Pots

These pots subjected to a hydraulic pressure of 2000 psi are about 4 ft in diameter. (See Fig. 11.)

Material Specification: A212, Grade B, 6-in. shell and solid head machined.

Type of Weld Kerf: Joint in shell-fabricated double U. $\frac{1}{2}$- x $\frac{1}{8}$-in. strip. Shell to head fabricated single U. $\frac{1}{4}$- x $1\frac{1}{2}$-in. strip. 8° slope on sides.

Weld Metal, Specifications and Size of Electrode: Electrodes—root welded with AWS E7016. $\frac{5}{32}$–$\frac{3}{16}$, $\frac{1}{4}$ in. diam. Welds completed using $\frac{3}{16}$-in. Oxweld No. 40 wire, Grade 80 Unionmelt composition. Multipass. A-C current in all cases.

Preheat: Preheated to 250–300° F.

Heat Treatment: Stress relieved at 1150° F for 1 hr per inch of thickness.

Difficulties Encountered: Slag in root of circumferential joint.

Degree of Restraint: Seam restrained longitudinally and thickness. Butt restrained in width and length, and thickness.

Reference 6c—Press Beds

Complicated sections are involved with the over-all dimension of the weldment 10 x 15-ft cross section and 15 ft long.

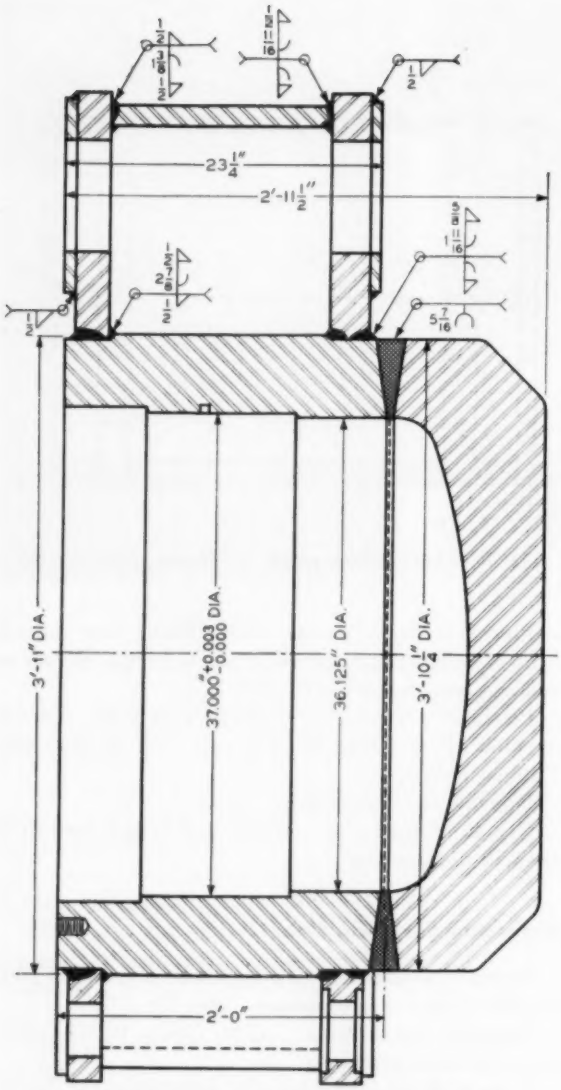


Fig. 11 Welded heavy ram pots

Type of Weld Kerf: Joint in plate-fabricated double-U, 1/2- x 1/8-in. strip. Manual and submerged arc welded. All other joints single-V or double-V. All web welds made with submerged arc. Top and bottom plate welded to webs manually.

Weld Metal, Specifications and Size of Electrode: Submerged Arc. Oxweld No. 36 wire, 3/16 in. and Grade 20 Unionmelt composition. Manual, AWS E6016 and AWS E6020-30. A-C current.

Preheat: None.

Postheat: Intermediate stress relief. Final stress relief.

Difficulties Encountered: Weld cracking, web and web to top and bottom plates. Intermediate stress relief of bottom plate and web assembly necessary.

Degree of Restraint: Web subassembly structure, i.e., welds all going in one direction restrained by thickness of material. Web to top and bottom plates is a structure having welds in two directions, perpendicular to each other. Restrained by thickness and web structure.

Reference 6d—Press Bed Repair

Material 18 in. thick, 8-in. diam hole cut 5 in. from edge. Section cracked from hole to edge.

Type of Weld Kerf: Fabricated single-U. 1/2-in. root width. 1/4- x 1-in. backing strip removed.

Weld Metal, Specifications and Size of Electrode: E6016 and E6020-30 electrodes. Approximately 1/8-in. thick layer of weld metal. AC, 3/16, 7/32 and 1/4, 5/16 in. diam. Peened.

Preheat: 250° F maintained until weld complete.

Postheat: Stress relief heat treatment.

Difficulties Encountered: None.

Degree of Restraint: Width and length due to thickness.

Reference 6e—Cross Head—Press Base

This cross head is of a complex shape with over-all dimensions approximately 11- x 4- x 4 in. and material from 2 1/2- to 3 1/2-in. thick welded.

Material Specification: ASTM SA201, Grade A.

Type of Weld Kerf: Joint in plate-fabricated double-U. 1/2- x 1/8-in. strip. Manual and submerged arc welded. All other joints single V or double V. All web welds made with submerged arc. Welds between top and bottom plates and webs manually welded.

Weld metal, Specifications and Size of Electrode: Submerged Arc. Oxweld No. 36 wire, 3/16 in. diam and Grade 20 Unionmelt composition. Manual, AWS E6016 and AWS E6020-30. A-C current.

Preheat: None.

Postheat: Stress relief heat treatment.

Difficulties Encountered: Some of the welds between web and top and bottom plate cracked or pulled out of plate.

Degree of Restraint: Web subassembly structure, i.e., welds all going in one direction restrained by thickness of material. Web to top and bottom plates is a structure having welds in two directions, perpendicular to each other. Restrained by thickness and web structure.

Reference 6f—Mill Coupler Repair

Material Specification: Heat treated, SAE 4140, chromium-molybdenum alloy steel. A mill structure, approximate weight 8 tons with sections 6 1/2 in. thick.

Type of Weld Kerf: Fabricated single-U in circumferential joint between two forgings, prepared by machining. 6 1/2-in. thick, 1 1/2- x 1/4-in. backing strip. 12-deg slope on sides.

Weld Metal, Specifications and Size of Electrode: Electrodes—root welded for depth of about 1 1/2 in. with AWS E9015 electrodes. 3/16 and 1/4 in. diam. Finished by multipass submerged arc, using 3/16-in. carbon-molybdenum wire.

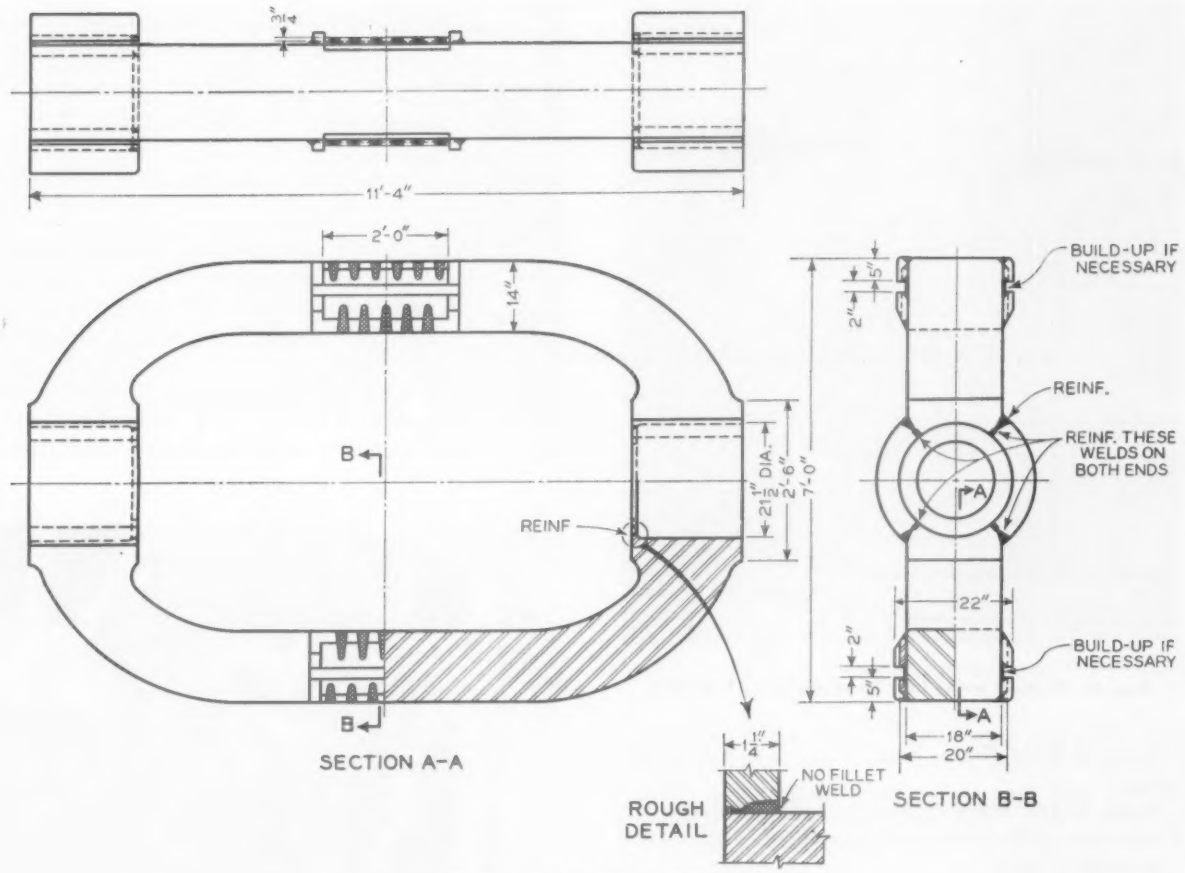


Fig. 12 Welded Gimbal Ring

Preheat: 500 to 600° F.

Postheat: Once coupler was preheated, it was not allowed to cool, and was placed in furnace at 500° F and raised to 1125° F (50° below original draw temperature) held for 1 hr per inch of gage. Furnace cooled.

Reason for using a weld metal of lower strength than base metal was to obtain maximum ductility with as high strength as practical. The thinnest section under stress was $2^1/_2$ in. the welded section was $6^1/_2$ in. it was felt the strength could be sacrificed for added ductility.

Difficulties Encountered: Magnaflux revealed crack in welds which held braces across joint for assembly. These welds were made without preheat. They were ground out.

Degree of Restraint: Restrained in width and length because of thickness.

Reference 6g—Gimbal Ring (See Fig. 12)

Type of Weld Kerf: $4^5/_8$ to 18 in.—machined double-U.

Weld Metal, Specifications and Size of Electrode: Electrodes—AWS E6020–30. $^5/_{32}$ through $^5/_{16}$ in. diam.

Preheat: 250° F. Kept hot until completed. Root preparations back chipped center.

Postheat: Stress relief heat treatment at 1150–1225° F.

Difficulties Encountered: Some cracking.

Restrained in width, length and thickness.

Reference 6h—Hydroelectric Throat Rings

Some of the welding consists of sections $3^1/_4$ in. thick. These throat rings are approximately 24 ft in diameter and of complex design. Representative welds in heavy sections are indicated in Fig. 13(*a*), (*b*) and (*c*).

Type of Weld Kerf: The joints in shell were single-V back chipped. Flanges fabricated single J.

Weld Metal, Specifications and Size of Electrode: Electrodes—All AWS E6020–30. $^5/_{32}$ through $^3/_8$ in.

Preheat: No preheat.

Postheat: Stress relief heat treatment.

Difficulties Encountered: Slag in some welds. Some cracking of heavy welds.

Degree of Restraint: Restrained in width and length.

Reference 6i—Half Rings

These half rings are approximately 20 ft in diameter and involve sections $3^1/_2$ in. thick. A typical weld is shown in Fig. 14 (*a*) and (*b*).

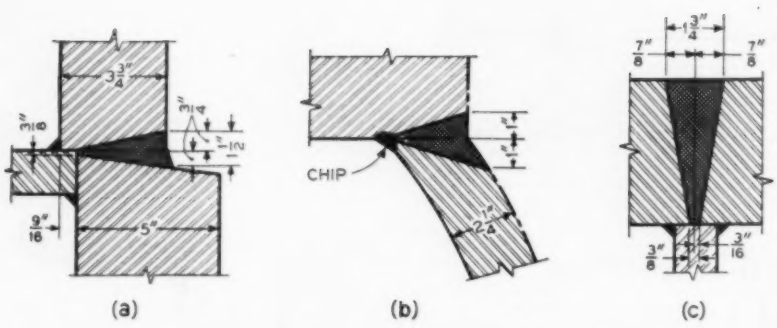


Fig. 13 Welded joints for hydroelectric throat ring

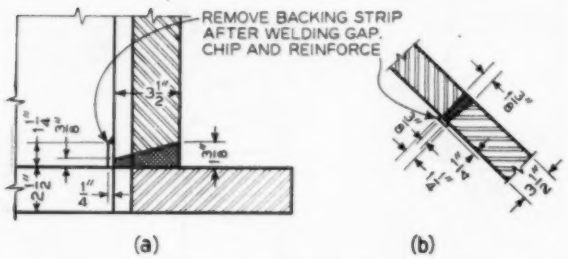


Fig. 14 Typical welds in a large diameter half ring

Type of Weld Kerf: Single J. $1\frac{1}{2}$ to $3\frac{1}{2}$-in. material.

Weld Metal, Specifications and Size of Electrode: Electrodes—AWS E6020-30 throughout. All sizes.

Preheat: None.

Postheat: Stress relief heat treatment.

Difficulties Encountered: Hard to keep flange sections from distorting, therefore necessitating peening and block welding.

Degree of Restraint: Width and length gage.

Reference 6j—Coal Bucket

These buckets are of complex construction as illustrated in Fig. 15. They are made of manganese-molybdenum, 80,000 psi tensile strength steel and a modified manganese Hadfield steel of varying thicknesses.

Reference 7a—Flanges and Reference 7b—Hearth Frame

Details are given in the Summary Table. See also Fig. 16.

Reference 8a, b and c—Boiler Drums

In the fabrication of steam drums for high-pressure boilers, 3 types of weldable steels, made by the open-hearth or electric furnace processes, are usually used. These boiler quality steels include plain carbon (type 50SS), manganese-vanadium and chromium-copper types. Their chemistry and properties are summarized in Table 2.

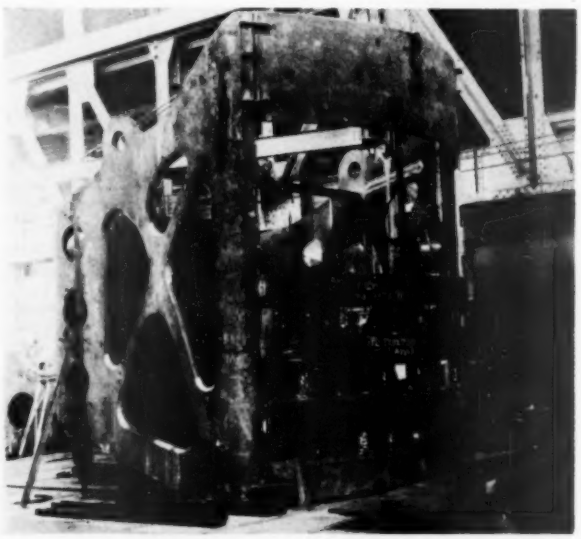


Fig. 15 50-yd bucket—second subassembly operation consisting of side walls, lip plate, heel plate, back wall, reinforcing channels and half rounds

Sections are cut from both ends of the boiler plate for test samples which are examined for soundness and structure. These tests are performed in accordance

Table 2—Typical Composition and Transverse Properties of French Boiler Steels

	Chemical analysis, %							*Mechanical properties*				
Steel	*C, max*	*Mn*	*S, max*	*P, max*	*Cr*	*Cu*	*V*	*Yield point, psi × 10³*	*Tensile strength, psi × 10³*	*Elongation, %*	*Reduction in area, %*	*Impact, kgm/cm²*
C steel (50SS)	0.20	1.0	0.03	0.04	...	...	...	38 min	67/80	20 min	..	4 6* min
Cr-Cu (AC-54)	0.20	1.0 max	0.03	0.04	0.45	0.45	...	50	77/91	20 min	60	4 6* min
Mn-V	0.20	1.2/1.5	0.03	0.04	...	...	0.15	47	77/91	20 min	75	4 6* min

* Longitudinal.

N.B.—All of the above materials are supplied in the heat-treated condition, which usually consists of normalizing at 1560° F followed by tempering at 1200 F.

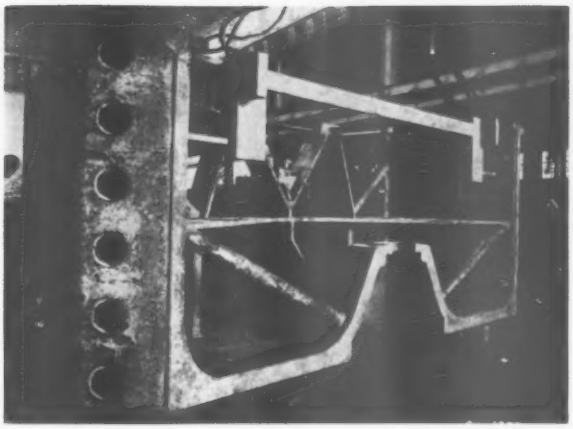


Fig. 16 Welded hearth frame

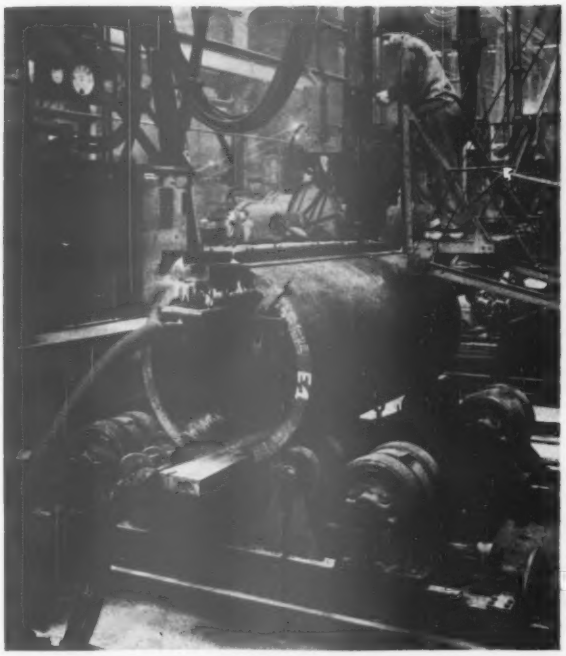


Fig. 18 Preheating boiler drum

with the code requirements for the specific material. The use of ultrasonic equipment for checking the soundness of plate materials is steadily gaining in popularity.

The longitudinal welds in the drum are executed by the Unionmelt process. For thicknesses up to about 4 in., the first pass is deposited on the ID and the second on the OD of the drum. The courses comprising the drum are hot press formed and then a "K" type welding groove is machined in them (see Fig. 17(*a*) for details of groove).

Longitudinal welds in drum plate 4 in. and more in thickness may also be consummated by using the multipass process, in which case the welding groove is machined as per details given in Fig. 17(*c*).

The butt welds, used to join together several short sections of the shell, or to join the heads to the drum, are made by manual metal arc for thicknesses up to 4 in. (see Fig. 17(*b*) for type of groove used). For thicknesses greater than 4 in., multipass automatic welding (with flux) is employed.

In the above welding operations the work is carefully preheated with gas jets to 500–600° F. (Fig. 18.) The weldments are postheated in the same fashion after welding and allowed to cool in still air.

The individual sections comprising the drum are welded longitudinally and are then subjected to radiographic examination. Any repair that may become necessary is done by manual welding. As a final treatment, the finished section is either stress relieved at about 1200° F or is normalized and tempered. (Fig. 19.) This procedure varies somewhat with different materials, consistent with the specific welding procedure used and inherent characteristics of the base metal, so as to insure the highest attainable properties in the weldment.

When the drum has been completed, i.e., after the shell sections have been welded together and the heads have been joined to the shell, the entire weldment is once again stress relieved at 1200° F. (Fig. 19.) In all of the above operations, deformation is held to a minimum by careful groove preparation and by using

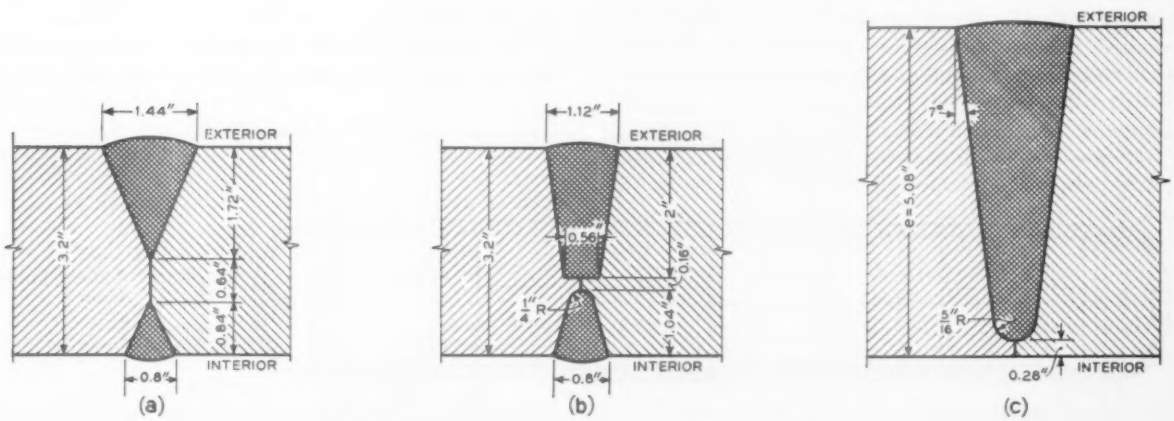


Fig. 17 Welding grooves for boiler construction

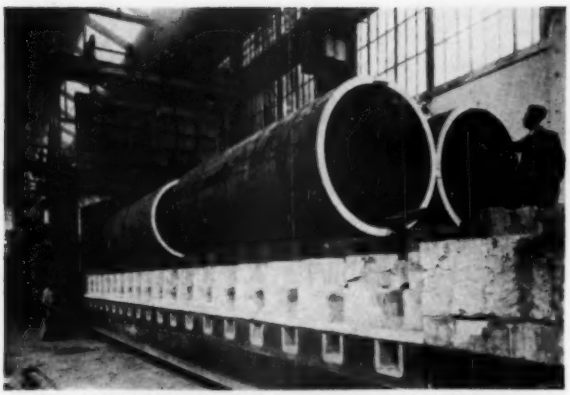


Fig. 19 Postheating boiler drum shells in furnace

suitable fixtures to hold the work rigidly clamped. Completed drums are shown in Fig. 20 and the size of a typical drum in Fig. 21.

Reference 9a—Rolls

The Inland Steel Co. welds stub ends to rolls, replacing worn or broken journals of rolls, anywhere from 2 to 26 in. in diameter. The maintenance shop does not have too many duplications and depends on past experience of trial and error as a guide. They employ back-to-back method, prebend, counter and restraining stiffness. They rely on definite predetermined welding procedures.

Stub ends up to 6 in. diam are manually arc welded. Above this diameter the submerged arc process is employed. In some cases, depending upon chemical composition, thermit welding is used.

On welds made in multiple passes, a pneumatic peening hammer is employed to clean and peen each bead. In many cases double-V joints are welded in a vertical position with two welders working opposite each other.

Visual inspection only is employed during the course of welding and after completion. Whenever feasible and practical, the welded fabrication or casting is stress relieved by heating in an annealing furnace to 1150° F and maintained at the above temperature for 1 hr for each inch of thickness of the heaviest cross section.

Employing the above practices and procedures has resulted in very satisfactory performance. Failures have been practically nil and the fabrications and castings have withstood satisfactorily the loads for which they were designed.

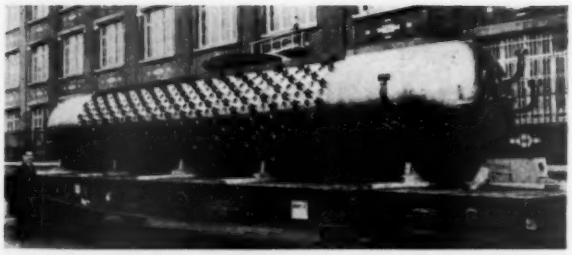


Fig. 20 Completed boiler drum

Reference 9b—Plates and Slabs

In using single- and double-V, U joint and J joints and combinations of the above, the joints were so prepared that complete penetration with a minimum amount of weld was obtained whenever necessary. Sketches of some of the jobs in which above joints have been used successfully are shown in Figs. 22 and 23. Most joints are prepared by torch cutting. The heavier sections above 6 in. are J and U grooved by machining.

In fillet and butt joints above 18 in. in length the submerged-arc welding process is used. Under that distance the manual arc welding is employed. Again in special cases and intricate shapes on large castings thermit welding is used. In welding deep grooves, one-third of the depth of the vee is first welded from one side, the plate reversed and two-thirds welded on this side. It is then turned over and completed on the first side that had been only one-third welded. The final passes are then made on the second side or the two-thirds completed side.

Reference 10—Pipe

An article in the December 1950 issue of THE WELDING ENGINEER describes the welding of tubing $3^{3}/_{8}$ in. wall thickness, 14 in. in diameter and made of $2^{1}/_{4}\%$ Cr – 1% Mo steel. This piping is to operate at 1800 psi and 1050° F.

The selected groove incorporates a chrome-plated backing ring of the same material as the pipe. The

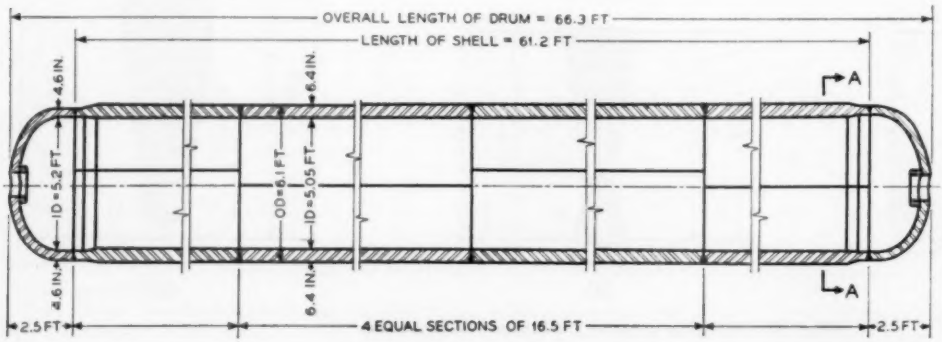


Fig. 21 Welded boiler drum

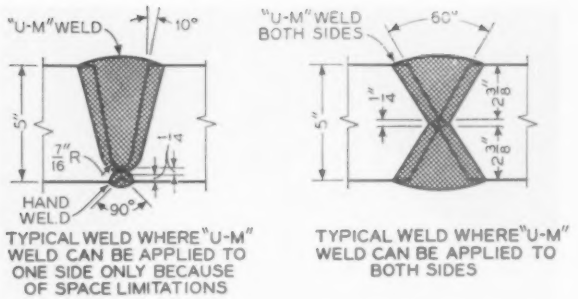


Fig. 22 Joint details in welded plates and slabs

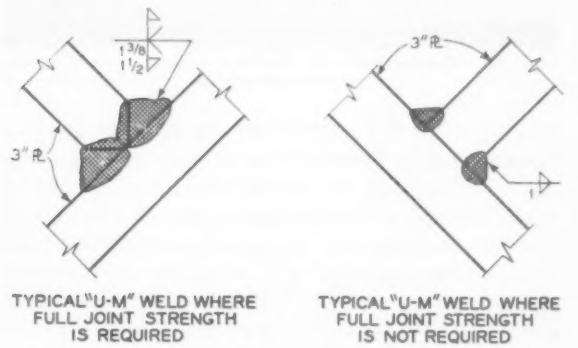

3/8
1 1/2
3" R
TYPICAL "U-M" WELD WHERE FULL JOINT STRENGTH IS REQUIRED
3" R
TYPICAL "U-M" WELD WHERE FULL JOINT STRENGTH IS NOT REQUIRED

Fig. 23 Joint details in welded plates and slabs

backing ring and the mating pipe ends have a 5-deg taper. The groove has a $^3/_{16}$-in. root gap to provide room for a good clean root pass. The pipe terminates in a $^1/_{16}$-in. wall at the root gap in preference to the sharp edge used by some manufacturers. This feature prevents damage to the pipe ends and interference with the backing ring fit during field erection.

The groove starts with a cutback on a $^3/_{16}$ in. radius and advances with a 20-deg vertical cutback tangent to the circular portion. At a point $1^3/_4$ in. out from the inner wall, the cutback changes from 20 to 10 deg with the vertical. Up to this point, the groove is identical with that previously used on schedule 160 tubing (both carbon-molybdenum steel and 1% chrome – $^1/_2$% molybdenum steel), on which the welders were already qualified.

This welding groove provides an opening $2^1/_8$ in. wide at the outer surface of the 14-in. (nominal size) tubing and an opening $1^7/_8$ in. wide at the outer surface of the 10-in. tubing.

The number of passes using an E9015 electrode were as follows:

14-in. tubing	
Fixed horizontal	76 passes
Fixed vertical	225 passes
10-in. tubing	
Fixed horizontal	35 passes
Fixed vertical	128 passes

Electrode consumption averaged 160 lb per joint in the 14-in. tubing and 75 lb per joint in the 10-in. tubing.

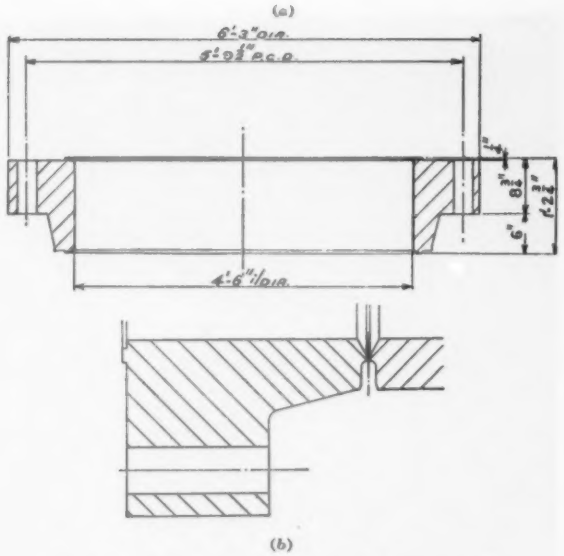


Fig. 24 Welding of flange and oil separators

Preheating and stress-relieving cycles were performed using gas-heating rings, stranded copper wire induction coils and induction-heating coils of water-cooled copper tubing. The most uniform heat control with the least use of electricity was obtained with 27 turns of 500,000 circular mil stranded copper wire. The wire was jacketed with woven asbestos turing and separated from the pipe by two layers of $^1/_8$-in. asbestos paper. With this arrangement, the stress-relieving temperature of 1350 ± 25° F could be readily reached and maintained. Also, the cooling rate of 300° F per hour could be very closely controlled.

Reference 11—Flanges of Oil Separators

The firm of G. A. Harvey, London, had a number of gas and oil separators 4 ft 6 in. ID for 1500 psi working pressure. Each unit was made from two 3-in. thick cylinders, each made up of two rolled belts with a heavy forged flange ring (Fig. 24*a*) welded on at each end. These flanges were 6 ft 3 in. in diameter overall, and $8^3/_4$ in. thick at the bolt circle, and weighed about three tons each. The weld preparation is shown in Fig. 24*b*.

The whole of the welding was subject to X-ray examination.

Initial trouble was encountered in welding on the flanges. Cracks appeared in the flanges in the vicinity of the weld. The cracks ran parallel to the circumferential seams a short distance inside the nose of the flange spigot. An immediate investigation was made to ascertain if the trouble was due to the quality of the steel forging, or whether it arose from some other cause such as the extra restraint of the heavy flange

The following was the analysis of the material: Carbon, 0.23%; silicon, 0.20%; manganese, 0.59%; sulfur, 0.039%; phosphorus, 0.036%; nickel, 0.18%; chromium, 0.14%.

All the cracks took place in the forged flanges and none has ever occurred in the rolled plate. They passed

Fig. 25 Macrograph of automatically produced butt weld of 4 1/4-in. thick plate

circumferentially round the seam and traveled from the side of the inner weld at right angles through the nose of the preparation. The forged ring had a normalized ferrite-pearlite structure and was of normal cleanliness and moderate grain size.

Sulfur prints showed no undue segregation, and the sulfur content was confirmed at 0.037%. The heat-affected zone contained no martensite and had a maximum hardness of 220 Vickers Diamond Hardness.

The crack usually originated near the edge of the heat-affected zone, and then traveled for the majority of its length well outside the heat-affected zone, showing that it was not due to weld hardening. It was abnormal in character containing many subsidiary families of fine cracks on either side of the main separation. The main cause of cracking was probably the severe restraint caused by the heavy flange combined with steep thermal gradients. Preheating the flange to 100° C has eliminated the cracking. This additional heat would reduce the effects of all the factors discussed.

As the Code requires stress relieving these units at 650° C, it is felt that this will somewhat improve the impact values of the steel forgings.

Reference 12—Diesel-Electric Locomotives

The welding of such an important part as an underframe of the Diesel-electric switcher is governed by fundamental variables in automatic welding, namely, current intensity, current voltage, velocity of welding, size and type of welding rod and chemistry and grain size of welding flux. Practically all these welds are fillet welds. Their size ranges from 1/2 to 3/4 in. The diameter of the electrode is ordinarily 1/4 in. and the travel somewhere between 7 and 12 ipm. The voltage

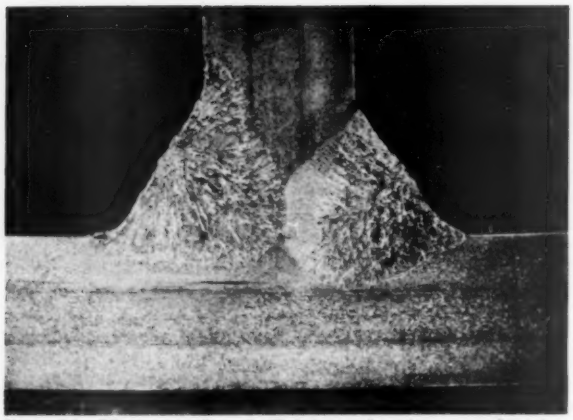


Fig. 26 Macrograph of T-joint showing overlapping automatic welds

is about 33 to 36 v and the current intensity at transformer 800 amp. The thickness of the plate ranges from 1 1/2 in. to as high as 4 1/4 in. Occasionally, heavier base plates are required in order to obtain definite loads upon the power axles. In such instances it was found advisable to butt weld 4 1/4-in. plates together, thus producing a plate 40 ft 3 in. long, 10 ft wide and, as mentioned, 4 1/4 in. thick. Two single automatic butt welds laid at speeds of about 5 to 6 ipm make this specified joint (see Fig. 25). In order to overcome the latter mentioned internal notch effect, the beads overlap, thus representing practically one solid plate. The chemical analysis of the parent metal and the automatic welds was as follows:

	Parent metal	*Weld metal*
Carbon, %	0.21	0.20
Manganese, %	0.47	0.46
Silicon, %	0.01	0.38
Phosphorus, %	0.022	0.017
Sulfur, %	0.019	0.021
Nitrogen, %	0.017	0.008
Silicates, %	0.008	0.005

In order to eliminate cracked welds, the following thoughts must be incorporated:

1. Elimination of any internal notches, i.e., full overlapping welds throughout the entire section (see Fig. 25).
2. Ample radii in finishing the welds to approach a distribution of stresses indicated by the well-known photoelastic work by Everett Chapman and others (see Fig. 26).
3. Positively no undercutting.
4. Clean surfaces of contact between welding parts.
5. Deep penetration also helps to minimize unwelded sections.
6. Stress relieving shall be encouraged as further measure of ultimate safety of the welded assembly.

Reference 13—Pressure Vessels

A very brief article mentions the welding of 5 1/2-in. plates into pressure vessels using the automatic arc. No details are given.

Reference 14—Fabricated Press Frames

The article cited in this reference, with a minimum of details, describes the use of an automatic welding process which depends on the ability of a heavily coated manual type electrode to give good welds while the edge of the coating rests on the workpiece. A special holder maintains the electrode in contact with the work while a second holder placed in the rear of the first begins welding as soon as the first electrode has been deposited. Electrodes from $^1/_4$ in. diam up to $^1/_2$ in. diam may be used. The particular application involves frames from 2 to 5 in. in thickness. No preheating or stress relieving is mentioned.

Reference 15—Press Frame

Figure 27 shows the methods used by the Cleveland Crane & Engineering Co. in welding heavy presses and shears. They use both manual and automatic welding. When the submerged arc process is used, they employ a single pass. Seam welds in I-beams and box sections are welded by making two welds simultaneously. They point out that proper sequence for shop fabrication is important. They employ a 300° preheat before welding of heavy plates.

Reference 16—Pressure Vessels

A short article in THE WELDING JOURNAL describes the fabrication of a sphere of welded stainless steel for rocket-propelled aircraft experiments by the U. S. Air Force. The vessel is stressed to contain 200 gal of liquid nitrogen at 5500 psi pressure and at −340° F with a comfortable safety tolerance. The sphere measures 54 in. in diameter, weighs approximately 7500 lb and is made of stock measuring $3^{11}/_{16}$ in. in thickness. The plates were pressed to shape in a special die after preheating them to 1700° F. An interesting innovation in layout enabled the shaped plates to be both trimmed and beveled in the same operation.

A heavy-duty grinder was used to remove slag and residue from the trimming operation. To insure against contamination, the parts also were pickled.

The parts were then fitted and held temporarily in place with thin backing plates. Attesting to the quality of the workmanship, all segments conformed symmetrically, and there was no joint misalignment.

First weld passes were made with Heliarc welding. These welds were then subjected to inspection by both Gamma ray and Dy-Chek.

Following inspection of the first weld passes, the joints were filled by multiple-pass arc welding. General Electric Type 1347, $^3/_{16}$-in. diam coated electrode metallic arc rod was used. In all, nearly a ton of weld rod was used on the job. The combined Gamma ray and Dy-Chek inspection processes were repeated several times as the welds were built up.

Final operation was application of heat treatment to achieve a full annealed condition. The sphere was heated at 1950° F for $3^1/_2$ hr and then subjected to both internal and external quenching through use of high pressure jets. It was then subjected to hydrostatic test at 10,000 psi pressure in a test chamber.

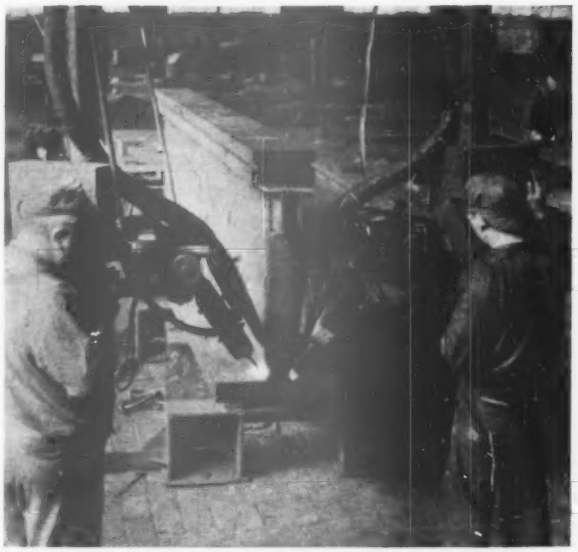


Fig. 27 Welding heavy press

Reference 17—Pressure Vessels

In a general article describing the submerged-arc welding applications in pressure vessel fabrication, N. G. Schreiner gives some general information. The following material is reproduced as being in accord with good practice:

Preheating is recommended on heavy-walled vessels, especially on girth seams where the metal is in excess of $1^1/_4$ in. in thickness. This is especially necessary when high-tensile steels, such as the SA212 type, are being welded. The preheating temperature should be at least 200° F and should be increased as the thickness increases, or as the diameter of the vessel becomes smaller, the thickness remaining essentially the same. An intermediate stress-relieving operation may be advisable at the half-way point of a single-U butt joint thicker than 6 in. to alleviate stress concentrations, although opinions differ on the need of such treatment. Single-U butt joints 5 in. thick in alloy steel have been produced in quantity with suitable preheat and without intermediate stress relief. The larger instantaneous heat input of a submerged melt weld permits the use of lower preheat temperatures than are required for manual arc welding.

Reference 18a—High-Pressure Boiler Drums $3^1/_8$ In. Thick

Type of Weld: Submerged Arc. Single-U groove butt weld, 15 deg total angle, $^5/_{16}$ in. radius, $^1/_4$-in. root face, $3^1/_8$ in. deep. Multilayer Tandem—37 passes.

Welding Conditions: Current—1080 amp at 28–29

v, lead wire; 1080 amp at 34–35 v, trail wire; 1350–1410 amp, ground. Wire—Oxweld 40, 1/4 in. diam, spaced 1 in., center-to-center—8.69 lb./ft (angle-lead vertical, trail 20 deg from vertical, plowing).

Welding Composition—Unionmelt, Grade 80. Size 20 XD. Power—3- to 2-phase modified Scott circuit. Speed—50 ipm. Passes—2 to 35—Wire positioned 1/8 in. from surface of groove to surface of wire. Pass—36—Wire positioned 1/4 in. from surface of groove to surface of wire. Pass—1 and 37—Wire positioned on center line of groove.

Reference 18b—6000-Lb Compressor Cylinder 3 In.

Single-U groove, 15-deg V., 1/4 in. radius, 3/16-in. root face. Preheat 450°.

Welding Conditions: Current—575 amp at 30 v. Wire—Oxweld 36, 1/8- and 4/32-in. diam wire. Welding Composition—Unionmelt, Grade 90. Size 12 X150. Speed—24 ipm. Passes—50–55.

Reference 18c—Fabricating Heavy Presses

The flanges of some heavy I-beams for this press were 12 in. thick and 36 in. wide, rolled with a stub in the center so that the joint between the flange and the web was actually a butt joint. The web was also 12 in. thick. The beams were 40 ft long and 14 ft deep, weighed 180 tons each, and 2 tons of welding wire were used on each beam in making the 12-in. butt welds joining the two flanges to the web.

The double-groove joints were prepared with 3/8 in. radius at the bottom, a 1/4-in. thick butted root and a groove of about 10 to 15-deg-included angle. The deposits were made using the tandem welding technique with two 3/16-in. Oxweld No. 36 welding wire electrodes. Power was obtained from two transformers in the Scott connection with 950 amp and 32 v on the lead wire, and with 900 amp at 34 v on the trail wire. Speed of welding was between 27 and 30 ipm. The assembly was made with an angle between web and flange of about 95 to 96 1/2 deg on the side to be first welded. After 2 1/2-in. thickness of deposit had been made from one side, the 95 to 96 1/2-deg angle had changed to about an 89-deg angle. The entire assembly was stress relieved, after which the root was machined out and deposits begun from the second side, and the second side groove completely filled. The first side was then completed. For all of this work Grade 80 Unionmelt Welding Composition, Size 20 XD was used. Approximately 180 passes were required to complete the joint. The work was done with Unionmelt Type DSH welding heads. Preheating was accomplished and heat maintained by 4 rows of gas burners, with 1-in. diam burners, spaced approximately 24 in. on centers along the full length of the beam—one row applying heat to the flange on the outside, the other row applying heat to the underside of the web near the joint. Both joints were welded at the same time.

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Designing and Making Welded Structural Steel Members for Cyclic Loading

A report compiled by a Subcommittee of the Fatigue of Welded Joints Committee, WRC, suggesting precautions to be used in designing and making structures which are subjected to cyclic loading

INTRODUCTORY REMARKS

A CONSIDERABLE amount of design data has become available in the last decade on the actual strength of different types of structural members subjected to cyclic loading. These data cover butt and fillet joints in a wide variety of structural connections for loads varying from tension to compression, from zero to tension and from tension to tension. Most of the data has been compiled to cover life cycles of 100,000 and also 2,000,000, with methods of extrapolating indicated.

This report is not intended in any way to duplicate this type of information.

The Welding Research Council has been asked to launch a new investigation aimed at improving the endurance limit of welded joints subjected to cyclic loading by more favorable geometry, metallurgical considerations or both. As a preliminary step to the work of this Committee it was thought desirable to compile a list of helpful hints of the "do" or "don't" type, which, if observed, would improve fatigue strength but may not always be necessary, depending on service considerations.

Following the pattern of the organizational setup of the Fatigue Committee, these "do's" and "don'ts" have been arranged under the broad headings of Geometrical Effects and Design Considerations, and Metallurgical and Fabrication Considerations. Obviously, the classification is arbitrary and in some cases the "do's" and "don'ts" belong to both classifications.

The Committee would welcome any further suggestions of this type. These suggestions should be sent to the Secretary, W. Spraragen, Welding Research Council, 29 W. 39th St., New York 18, N. Y.

GEOMETRICAL EFFECTS AND DESIGN CONSIDERATIONS

Suggested "Do's" in so far as Practical

1. Change sections gradually and avoid re-entrant corners.
2. Where welding joins different thicknesses of plates, or sizes of plates or sections, provide a gradual transition.
3. Grind butt welds flush and smooth.
4. Use butt joints instead of lap joints.
5. Extend cover plates on girders well beyond theoretical cut-off points.
6. Align parts to avoid eccentricity.
7. Join parts of unequal stiffness with a gradual transition.
8. Streamline fillet-weld joints.
9. Give preference to structures with multiple-load paths, in which a fatigue crack in any one of several key members is not likely to precipitate sudden collapse of the whole structure.
10. Plan sequence of welding to avoid shrinkage cracks and other weld defects as well as excessive deformation.
11. Inspect joints after welding.
12. Locate welded joints where the fatigue conditions are not severe.

Suggested "Don'ts" in so far as Practical

1. Don't use plug or slot welds but instead use a hole or slot of adequate size with a fillet weld all around.
2. Don't use eccentric joints.
3. Don't use joints having large local variations in ability to deform under the applied load, e.g., the butt-welded connection of a beam to an unstiffened column flange, where the edges of column flange can deform easily and consequently the part of the butt weld opposite the web is overstressed.
4. Don't leave undercuts or other abrupt changes of geometry.
5. Don't introduce high restraint in localized regions.
6. Don't attach fittings, handles and bosses, or make openings at locations of high stress.
7. Don't rely upon the temporary expedient of arresting the propagation of a fatigue crack by drilling holes at the ends of the actual crack or by removing the material containing the crack but rather determine the cause and make an adequate repair.
8. Don't specify excessive welding.

METALLURGICAL AND FABRICATION CONSIDERATIONS

Suggested "Do's" in so far as Practical

1. Use welding procedures and methods that will eliminate internal defects, such as microcracks, gas pockets, and slag inclusions, as well as excessive surface ripples or roughness. To this end, in some cases it may be advisable to use low-hydrogen electrodes or submerged arc welding, or to preheat or administer a suitable post-heating treatment.
2. Use base metal and weld metal suitable for the service.
3. Strive for smoothness in all welded joints by avoiding undercutting, cracks, spatter and other surface imperfections which might serve as stress raisers.
4. Machine or otherwise dress the weld at critical locations, if necessary, to obtain satisfactory smoothness.
5. Use procedure for preparation, fitting and welding that will produce sound welds.
6. Provide proper maintenance, including adequate protection against corrosion, wear, abuse, overheating, improper lubrication and repeated overloading.
7. Use multipass cascade welding in making fillet welds on thick material.

Suggested "Don'ts" in so far as Practical

1. Don't leave undercuts.
2. Don't leave incomplete root penetration.
3. Don't use intermittent welding.
4. Don't leave end defects in fillet welds.
5. Don't permit promiscuous striking of the arc outside of the weld area.
6. Don't peen first or last layers of a weld.
7. Don't over-weld.

Effects of Temperature on Weld Metal Properties

Tensile characteristics of arc-deposited carbon steel weld metal for two covered electrodes over a temperature range of −300 to +1400° F

by J. Heuschkel

A THOROUGH search of the literature fails to reveal a suitable description of the specific relations between the common tensile characteristics of metal-arc-deposited low-carbon steel weld metal and the prevailing fabrication or possible service temperatures. The simple purpose of this paper therefore is to provide such information over a temperature range from −300 to +1400° F for two representative covered electrode weld metals; one containing 0.019% nitrogen by weight and the other containing 0.043% nitrogen. These nitrogen contents approach the lower and upper limits secured with widely used present-day commercial covered electrodes.

At temperatures below −70° F the upper yield point stress becomes higher than that of the nominal ultimate tensile strength when selecting maximum resisted loads from the load-deflection curves in the conventional manner. The sharp yield point characteristic of the stress-strain curves at the low temperatures gradually diminishes with increased temperatures and disappears entirely at about +500° F. Above that temperature there is no sharp yield point. Pronounced strain aging occurs at around +300° F, even for welds containing as little as 0.019% N.

Nitrogen content of the weld deposit is shown to have a pronounced effect upon the weld ductility, particularly at very low temperatures. Welds containing 0.043% N achieve full brittleness at −300° F, even when using an unnotched tensile specimen. Stress-relief annealing at +1200° F does not improve the ductility of these high nitrogen weld deposits at −300° F and therefore such extreme brittleness at that temperature is not due to hydrogen. Low (0.019%) nitrogen content welds retain nearly full ductility in the unnotched tensile test down to −300° F, although the strength-elongation product decreases and the fracture appearance changes to the square brittlelike type.

J. Heuschkel is with the Westinghouse Research Laboratories, East Pittsburgh, Pa.

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Fully brittle fracture occurs only when the yield stress becomes equal to the true stress at rupture.

Nitrogen is shown to be responsible for the nuclei of the common "fisheyes" in weld fractures under those conditions involving higher availability of that element in gaseous form.

TEST CONDITIONS

The two individual covered electrodes involved in this study were selected for test following an exploration of deposit analysis and weld properties secured at normal temperatures for a series of 13 such electrodes. Those chosen to demonstrate the several phenomena discussed are characterized by the deposit analyses and room temperature tensile properties of Table 1.

For each of the two electrodes, two 36-in. long multipass 40-deg single-V butt joints, Fig. 1, were made by P. T. Ehrhardt, a highly skilled welder, using the conventional conditions shown in Table 2. Each of the resulting four joints was cut into fourteen 2.38-in.

Table 1—Composition and Room Temperature Tensile Strengths of Weld Metals Investigated

	Electrode No. 1	2
Deposit Composition.*		
Nitrogen, %	0.019	0.043
Carbon, %	0.065	0.050
Manganese %	0.32	0.31
Silicon, %	0.09	0.15
Oxygen, %	0.087	0.069
Hydrogen, %	0.0012; 0.0006	0.0010; 0.0011
Deposit Properties:		
Upper yield stress, psi	60,000	68,400
Lower yield stress, psi	55,600	63,400
Yield stress 0.2% set, psi	58,200	67,000
Yield stress 0.5% set, psi	56,300	64,500
Nominal ultimate tensile, psi	68,150	77,600
True stress at fracture, psi	133,400	104,800
Elongation in 1.5 in., %	27.97	18.66
Reduction in area, %	60.0	30.8
Fracture	Cup-cone	Irregular; 4 fisheyes

* Deposit composition is influenced by electrode core wire and coating composition, by arc operating technique and by composition of base metal.

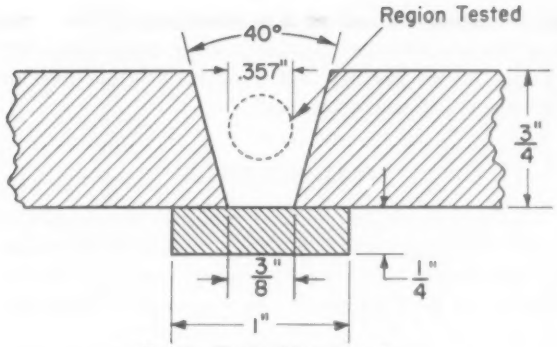


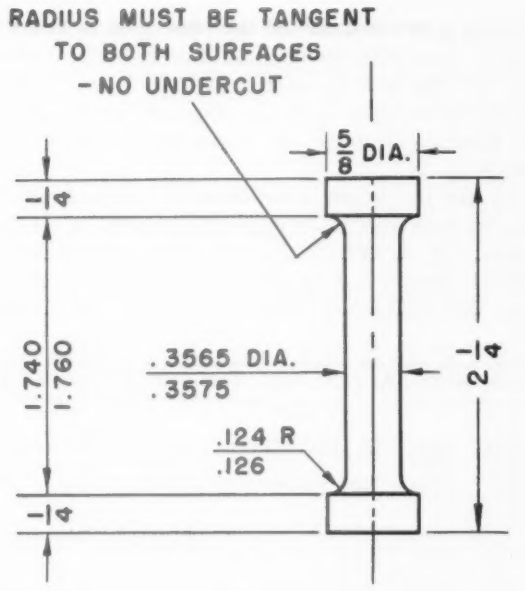
Fig. 1 Typical joint section

Table 2—Test Welding Conditions

Electrode No. →	*1*	*2*
Electrode diam, in.	5/16	5/16
Arc volts	32	23
Arc current, amp	220	220
Polarity	Straight	Straight
No. of layers	8	8

lengths and these blanks were machined into laboratory standard tensile specimens, Fig. 2, for testing in specially constructed laboratory apparatus, designed by Messrs. Olleman and Wessel. A feature of this machine is that continuous load-strain curves to rupture are provided at any desired test temperature between −300 and +1800° F.

The two sets of 14 as-welded specimens, made with the two electrodes, were tested at 14 different temperatures ranging from −300 to +1400° F; in 100-deg steps up to +600° F, and then in 200-deg steps up to 1400° F, Table 3. Only one specimen was tested at each temperature. The remaining two sets of 14 specimens for each electrode were divided into two groups of seven; one group was tested after a 1-hr 1200° F stress-relief annealing temperature across the −300 to +1400° F temperature range, Table 4 and the remainder of the specimens were tested at ambient temperature after a series of 1-hr thermal treatments ranging from −300 to +1200° F, Table 5. These treatments, by C. Mueller, were made in argon for all temperatures over 600° F and in air for the 200 to 500-deg bake-outs.



NOTE:
SPECIMEN AXIS MUST NOT DEVIATE FROM STRAIGHT LINE THROUGH END CENTERS BY MORE THAN 0.0005"

ALL DIMENSIONS ARE IN INCHES

Fig. 2 Short time, threadless, tensile specimen

Table 3—Results of Variable Temperature Tensile Tests of Covered Electrode Weld Metal in As-Deposited Condition

Test Temp. (°F)	Prop. Limit (PSI)	YIELD STRESSES (PSI) Upper	Lower	0.2% Offset	0.5% Offset	Nominal Ultimate Stress (PSI)	True Fracture Stress (PSI)	ELONGATION (%) Yield Point	Uniform	Total	Reduction in Area %	Product of Total Elong. and Nominal Ult. Stress X10⁻⁵	Fractures
DATA FOR SOUND DEPOSITS CONTAINING 0.019% H													
- 300	116500	120500	98600	118000	113000	101500	174400	2.92	9.40	25.60	56.80	2.60	Square; granular.
- 200	94200	98000	82700	95400	90400	87800	137000	9.40	15.88	29.59	45.00	2.60	Square; granular.
- 100	77500	78800	66100	77000	72800	77100	148400	5.39	19.19	36.31	60.33	2.80	Cup-Cone.
0	62900	64550	57100	63400	60700	70200	134200	4.25	18.93	31.91	59.00	2.24	Cup-Cone.
+ 79	59900	60000	55600	58200	56300	68150	133400	3.69	15.68	27.97	60.00	1.91	Cup-Cone.
200	48400	49750	-	50300	50400	67600	128000	2.83	13.04	19.58	55.30	1.32	3/4 Cup.
300	50200	51400	48900	-	-	88300	138700	1.60	13.35	19.80	44.75	1.75	1/2 Cup.
400	46900	46600	45900	-	-	89500	139600	1.45	13.80	26.77	48.22	2.40	1/2 Cup.
500	39350	-	-	41750	43400	82800	122100	-	15.50	30.04	42.50	2.48	1/2 Cup. loc. def.
600	31500	-	-	37600	41600	74700	136400	-	14.30	37.18	62.77	2.78	1/2 Cup.
800	29500	-	-	33500	36000	52600	104500	-	14.04	40.34	70.23	2.12	1/2 Cup.
1000	20700	-	-	24500	26200	31700	76500	-	11.62	48.24	80.49	1.53	1/2 Cup.
1200	10750	-	-	14350	15140	16430	47900	-	6.06	93.64	93.00	1.54	Cup.
1400	5680	-	-	7120	7320	7320	13800	-	3.60	95.30	87.80	0.51	Cup.
DATA FOR DEPOSITS CONTAINING GAS VOIDS AND 0.043% H													
- 300	140000	-	-	-	-	140000	140000	0.00	0.00	0.00	0.00	0.00	Gas void; brittle; no fisheyes.
- 200	105500	108500	94500	106550	101700	96100	124300	4.00	12.63	17.61	24.20	1.69	Gas void; brittle; no fisheyes.
- 100	83200	87000	77400	84800	80200	87600	112500	3.91	17.52	23.75	23.64	2.08	Gas void; brittle; no fisheyes.
0	72200	74400	67000	72400	68200	79200	90000	3.36	14.40	18.56	33.13	1.47	4 gas voids; diag. no fisheyes.
+ 79	67100	68400	63400	67000	64500	77600	104800	2.93	15.09	18.66	30.82	1.45	4 gas voids; fisheyes.
+ 200	63200	64500	61100	62800	62500	81000	128700	2.26	13.20	15.88	40.27	1.29	2 gas voids; irreg. shear, 1 fisheye.
300	59000	63700	57000	-	-	99000	117700	1.54	13.12	13.67	18.66	1.35	1 gas void; irreg. shear, no fisheyes.
400	59500	62700	57700	61650	59430	104000	132500	1.47	16.35	16.78	21.67	1.74	6 gas voids; 45° shear; no fisheyes.
500	40000	-	-	47200	51430	90200	113200	-	15.28	21.56	22.16	1.94	8 voids; fracture oxidized.
600	39300	-	-	45750	49450	82000	115700	-	15.33	34.59	38.13	2.84	6 voids; fracture oxidized.
800	35500	-	-	39000	40000	50300	96300	-	14.31	37.62	66.12	1.89	3 voids; 1/2 cup-cone; oxidized.
1000	26700	-	-	30800	31500	37900	82500	-	10.59	36.66	69.92	1.39	2 voids; irreg. shear oxidized.
1200	24100	-	-	25250	25500	25600	33500	-	0.73	25.17	52.97	0.64	4 voids; irreg. shear.
1400	8680	-	-	10700	10900	10950	1100	-	0.32	55.73	59.62	0.61	2 voids; irreg. shear.

Complete load-strain curves to rupture were made by W. H. Pryle for every specimen while at temperature, using a strain rate of 750% per hour. These curves, typical examples of which are shown in Figs. 3 to 5 inclusive, were automatically plotted by the test apparatus. In these curves the strain abscissas are reversed in direction from that conventionally used; also, the machine records plastic strain and therefore to secure a corresponding strain value

for any given load on the specimen it is necessary to project back from the point of interest on the curve to the base line in a direction parallel to the initial slope of the curve, which includes a machine factor. Except for these two features the load-strain curves are conventional.

At the intermediate subambient temperatures the tension tests were made in an environment of nitrogen vapor. At −300° F the specimens were tested in liquid nitrogen and at the elevated temperatures the tests were made in an environment of air, the specimen being heated in a surrounding furnace shell. In each case the specimen was held at testing temperature for 20 min before loading and temperature-time records were made for each specimen.

YIELD STRESSES

The magnitude of the load required to exceed the upper yield point was found to increase with decreases in temperature below the normal ambient of +79° F, Figs. 3 and 4. Conversely, the magnitude of that load

Table 4—Results of Variable Temperature Tensile Tests of Covered Electrode Deposits in 1200° F Annealed Condition

Test Temp. (°F)	Prop. Limit (PSI)	YIELD STRESSES (PSI) Upper	Lower	0.2% Offset	0.5% Offset	Nominal Ultimate Stress (PSI)	True Fracture Stress (PSI)	ELONGATION (%) Yield Point	Uniform	Total	Reduction in Area %	Product of Total Elong. and Nominal Ult. Stress X10⁻⁶	Fractures
DATA FOR SOUND DEPOSITS CONTAINING 0.019% N													
- 300	131000	134200	106200	131600	126400	110200	186800	1.85	7.89	17.23	55.23	1.90	Sq., Granular.
- 100	64800	77500	65500	76400	72300	75200	158500	6.75	20.00	40.38	66.77	3.03	Lip cup.
+ 75	53500	56000	51200	53300	51300	65400	143400	3.76	18.75	34.17	66.77	2.23	Cup-cone.
+ 200	46200	47700	45900	47100	46600	64000	132000	2.40	13.68	21.53	61.39	1.38	1/2 cup.
400	47000	-	-	47800	50400	89500	128500	1.33	14.24	21.75	37.64	1.94	45° irregular.
600	33600	34700	34000	34000	37000	70800	113700	0.26	15.54	36.88	51.01	2.62	45° irregular.
1000	21650	-	-	25600	27250	33350	87700	-	9.23	48.67	81.23	1.62	Cup-cone.
1400	6200	-	-	7660	7920	8040	15000	-	1.47	102.07	86.82	0.82	Cup-cone.
DATA FOR DEPOSITS CONTAINING GAS VOIDS AND 0.043% N													
- 300	145400	-	-	-	-	145400	145400	0.00	0.00	0.00	0.00	0.00	Sq., Gran.,1 void.
- 100	82700	87800	73500	86000	81200	83800	123000	3.68	14.77	25.69	41.99	2.15	Irreg., 1 void.
+ 75	63000	63700	58100	61000	58400	72100	127750	2.07	12.92	24.74	55.61	1.78	1/2 cup, 3 pits.
+ 200	60000	61000	56700	58500	57000	72500	110800	-	10.70	15.96	43.26	1.16	45° irreg.,3 pits.
400	54000	-	-	55900	59000	95500	121400	1.10	11.08	11.75	21.67	1.12	45° irreg.,4 pits.
600	44000	-	-	50100	54500	74600	108500	-	12.24	28.82	41.99	2.14	45° irreg.,2 pits.
1000	28400	-	-	31800	33500	36800	68600	-	4.29	24.70	64.13	0.91	1/2 cup, 2 pits.
1400	8750	-	-	10450	10600	10600	1560	-	0.50	73.96	68.05	0.78	Irreg., 1 pit.

Table 5—Results of Room Temperature Tensile Tests of Covered Electrode Weld Deposits After 1-Hr Hold at Various Temperatures

	Hold Temp. (°F)	Prop. Limit (PSI)	Yield Stresses (PSI) Upper	Lower	0.2%	0.5%	Nominal Ultimate Stress (PSI)	True Fracture Stress (PSI)	% Elongation Yield Point	Uniform	Total	Reduction in Area %	Fractures
DATA FOR SOUND DEPOSITS CONTAINING 0.019% N_2													
	- 300	56600	57300	52500	56100	54000	65500	115000	3.00	17.79	30.07	54.48	45° shear.
	+ 79	59900	60000	55600	58200	56300	68150	133400	3.69	15.68	27.97	60.00	Cup-Cone.
	+ 200	57800	58500	54000	56500	54100	66800	133400	3.86	16.76	29.86	62.08	1/2 Cup.
	+ 300	54000	54200	50400	53000	52100	65000	134800	3.15	18.10	32.68	63.79	1/2 Cup.
	+ 400	55500	56100	52200	54500	53500	66000	133900	2.76	17.46	29.37	62.43	1/2 Cup.
	+ 500	56300	57400	54000	56000	55000	68100	136200	3.51	15.31	26.97	61.04	1/2 Cup.
	+ 800	56000	56600	53000	55600	53800	66200	137700	3.82	18.74	34.60	64.46	Cup-Cone.
	+1150	53500	56000	51200	53300	51300	65400	143400	3.76	18.75	34.17	66.77	Cup-Cone.
Average	All 8	56190	57010	52860	55400	53760	66400	133470	3.44	17.32	30.71	61.88	
Std. Dev.		1829	1717	1644	1617	1549	656	7190	0.42	1.27	2.65	3.44	
Variation %		3.3	3.0	3.1	2.9	2.9	1.0	5.4	12.2	7.3	8.6	5.6	
DATA FOR DEPOSITS CONTAINING GAS VOIDS AND 0.043% N_2													
	- 300	68500	69000	63000	65700	63300	78000	122200	2.44	14.08	20.30	43.26	2 fisheyes, irreg.
	+ 79	67100	68400	63400	67000	64500	77600	104800	2.93	15.09	18.66	30.82	3 fisheyes, 45°
	+ 200	67500	68000	60600	65800	62700	73000	123000	2.56	14.65	25.69	52.57	1 fisheye, irreg.
	+ 300	63600	66900	60700	64500	60700	73800	91300	2.91	15.82	22.63	39.40	2 fisheyes, irreg.
	+ 400	62600	66400	60200	63500	60500	73000	111500	3.30	14.44	23.43	49.02	1 fisheye, irreg.
	+ 500	61700	62100	56300	59600	57100	71100	112200	2.62	15.81	25.50	46.00	5 pits, 45° shear.
	+ 800	57200	58200	51000	55300	51100	67000	114800	2.07	15.37	25.06	52.18	4 pits, 45° shear.
	+1150	63000	63700	58100	61000	58400	72100	127800	2.07	12.92	24.74	55.61	3 pits, 1/2 cup.
Average	All 8	63900	65340	59160	62800	59800	73200	113450	2.61	14.77	23.25	46.11	
Std. Dev.		3858	3445	3816	3677	3825	3296	10875	0.42	0.95	2.43	7.60	
Variation %		6.1	5.3	6.4	5.9	6.4	4.5	9.6	16.1	6.4	10.4	16.5	

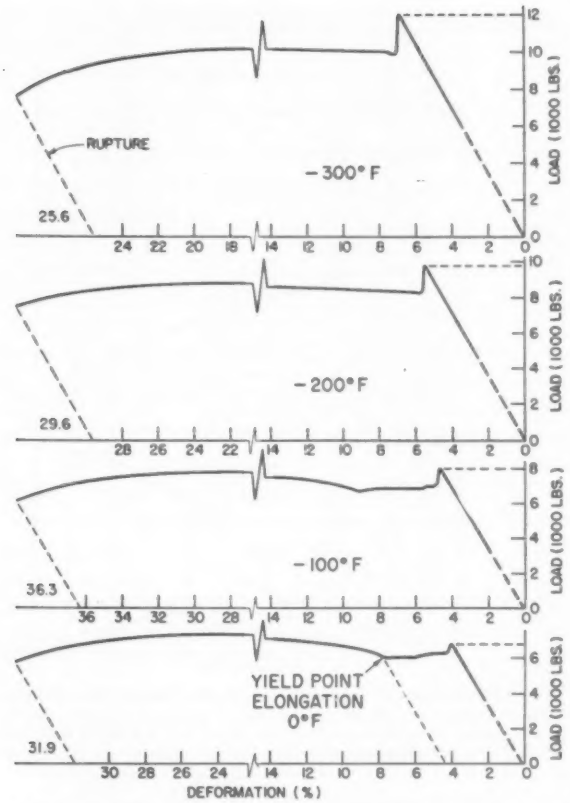


Fig. 3 Tension load-deformation curves for sound steel metal-arc welds containing 0.019% N and tested as deposited at the temperatures shown

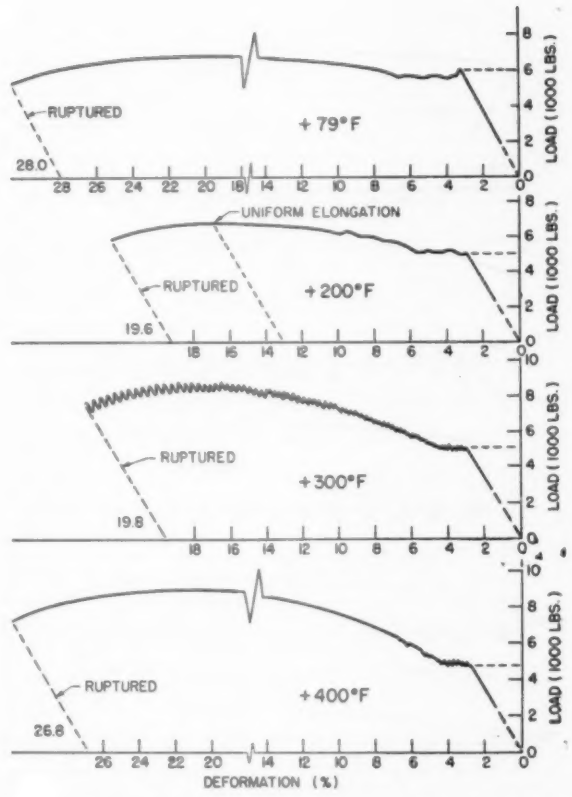


Fig. 4 Tension load-deformation curves for sound steel metal-arc welds containing 0.019% N and tested as deposited at the temperatures shown

decreased with increases in temperature up to about +500° F, at which temperature the characteristic of a sharp yield point entirely disappeared, Fig. 5. For these weld metals at temperatures above +500° F it is therefore necessary to use some specific amount of strain as a criterion of yield strength and the conventional values at 0.2 and 0.5% permanent set were used here. The use of 0.2 and 0.5% set as yield criteria results in the designation of stresses for the temperatures below +500° F which are intermediate between the upper and lower yield point stress values. These values are recorded in Tables 3 to 5 inclusive.

Both the specific and relative magnitudes of the upper yield point stress are of particular academic interest at temperatures of −100° F and lower. At and below −100° F, Fig. 3, the load at the upper yield point reaches a magnitude higher than that subsequently resisted. For temperatures of −100° F and lower, the stress at the upper yield point must therefore be taken as identical with the nominal ultimate tensile strength or it must be considered as exceeding the value of the nominal ultimate tensile strengths as conventionally determined from the load-strain curve. The latter alternative is chosen here.

The relations between yield stress and temperature for the low nitrogen welds in the as-deposited and the stress relief annealed conditions are shown in Figs. 6 and 7, respectively. There was no significant increase in yield strength within the +300 to +600° F temperature range for any of the welds although within that range substantial increases of ultimate strength occurred from strain hardening for both weld analyses in both conditions, Figs. 6 to 9.

For the higher nitrogen content weld deposits the upper yield stress approached the true stress at rupture as the test temperatures were reduced and was coincident with that value at −300° F, Fig. 8, a condition which resulted in a brittle fracture with no perceptible ductility. This phenomena persisted even after a 1-hr hold in argon at +1200° F, Fig. 9. For comparable conditions the higher nitrogen deposit had the higher yield stresses.

A practical application of the yield point data may result from the observation that there is a shift in type of yield point characteristic from a sharp upper value to a smooth elastic to plastic transition at about +500° F and above. This may be a factor in reducing residual stresses and could be pertinent in explaining some of the benefits achieved through preheating to or above 500° F. This is particularly pertinent since the yield strength of arc-deposited weld metal is high for its nominal composition. Since no sharp yield points exist at and above +500° F the shrinkage stresses induced from cooling have a better opportunity of adjustment at that temperature within the short time available for such adjustment than would be possible if

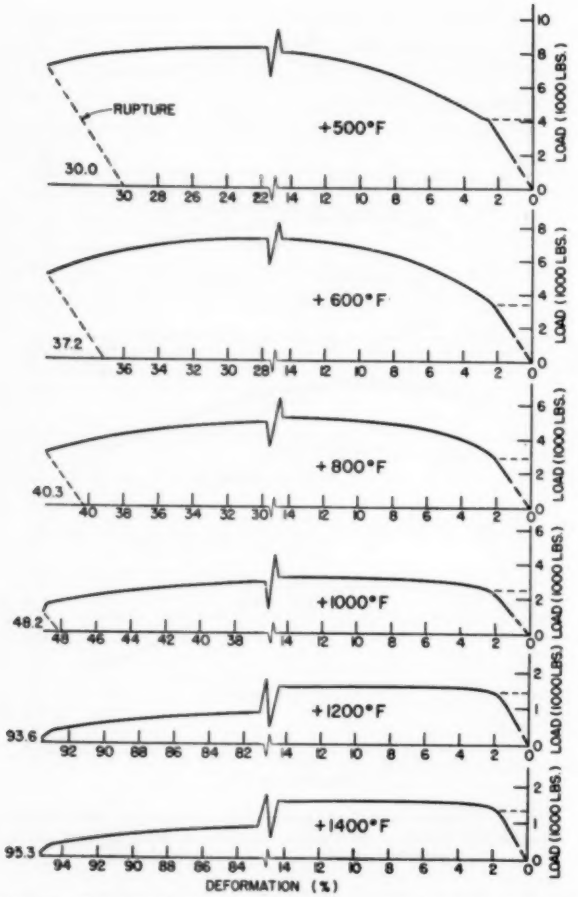


Fig. 5 Tension load-deformation curves for sound steel metal-arc welds containing 0.019% N and tested as deposited at the temperatures shown

a higher magnitude stress was required to produce plastic yielding.

NOMINAL ULTIMATE TENSILE STRENGTH

The magnitude of the nominal ultimate tensile stress, defined here as the maximum load resisted after the upper yield point had been exceeded divided by the original cross section area, became a maximum at the lowest temperature tested, −300° F, and a minimum at the highest test temperature, +1400° F, Figs. 6 to 9 inclusive, with intermediate maxima occurring at about +400° F, the iron-nitride precipitation strain-hardening temperature range. Minor variations in the magnitude of tensile strength for the two welds were found, but the same general relation of strength to temperature existed for the four series. In essentially all cases the higher nitrogen content deposits had the highest tensile strength. Since the deposit analyses were essentially identical except for nitrogen content,

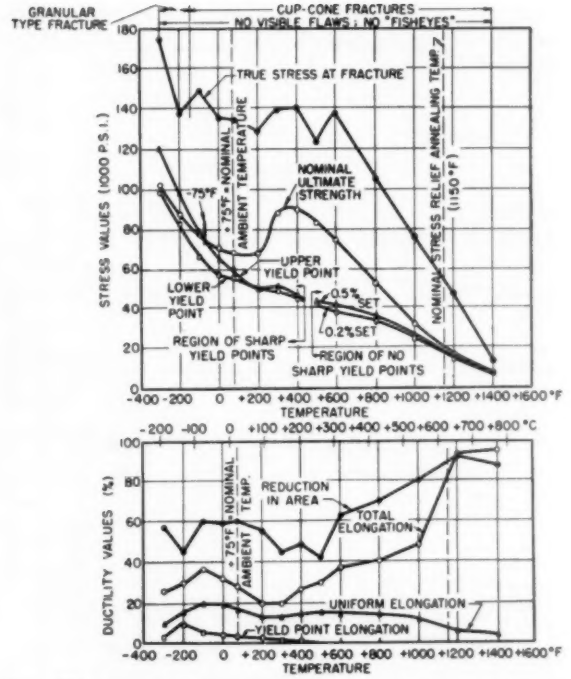


Fig. 6 Influence of temperature upon properties of manual, shielded metal-arc weld metal (covered electrode) in low-carbon steel deposit (C 0.06, Mn 0.32, Si 0.09, N 0.019%) with 0.357-in. diam unnotched tensile specimen (metal as deposited)

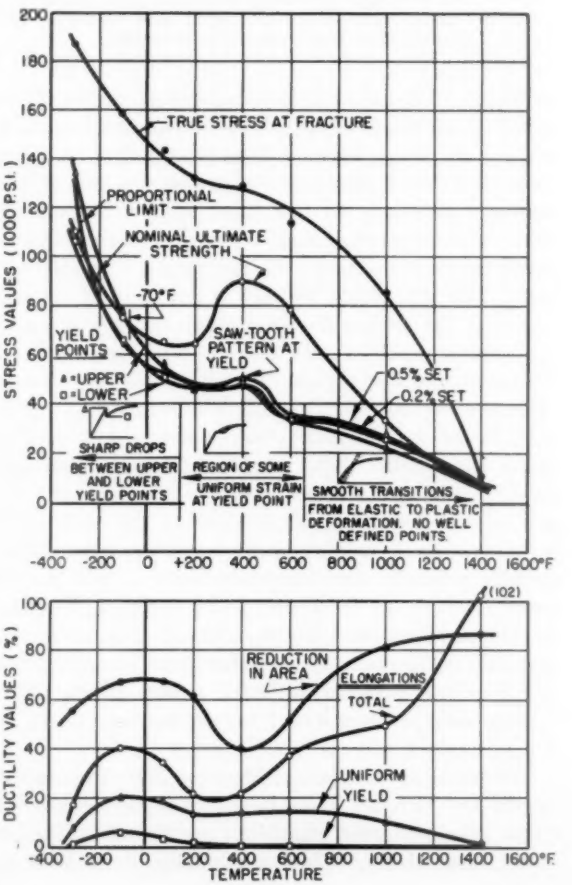


Fig. 7 (Right) Influence of temperature upon properties of manual, shielded metal-arc weld metal (covered electrode) in low-carbon steel deposit (C 0.06, Mn 0.32, Si 0.09, N 0.019) with 0.357-in. diam unnotched tensile specimen (metal stress relief annealed 1200° F)

Table 1, the differences in strength are presumed to be due to the influence of iron nitrides.

TRUE STRESS AT FRACTURE

The load being resisted immediately prior to initiation of rupture divided by the final smallest circular section of the specimen upon reassembly after fracture is taken as the true stress at fracture. For the 0.019% nitrogen content welds the magnitude of this stress is substantially above the nominal ultimate tensile strength for all temperatures less than +1400° F, Figs. 6 and 7. A high of 186,800 psi at −300° F was reached for the 0.019% nitrogen weld in the stress-relief annealed condition, Fig. 7. The decrease in magnitude of the rupture stress was fairly gradual from the maximum at −300° F to about +600° F, above which temperature this stress rapidly approached a low at +1400° F.

For the 0.043% nitrogen welds the true stress at fracture became no greater than that of the upper yield point stress at −300° F, Figs. 8 and 9, with the consequence that fracture occurred at that temperature without appreciable ductility. Across the entire temperature range the true stress at fracture was less than for the corresponding value of the lower nitrogen welds,

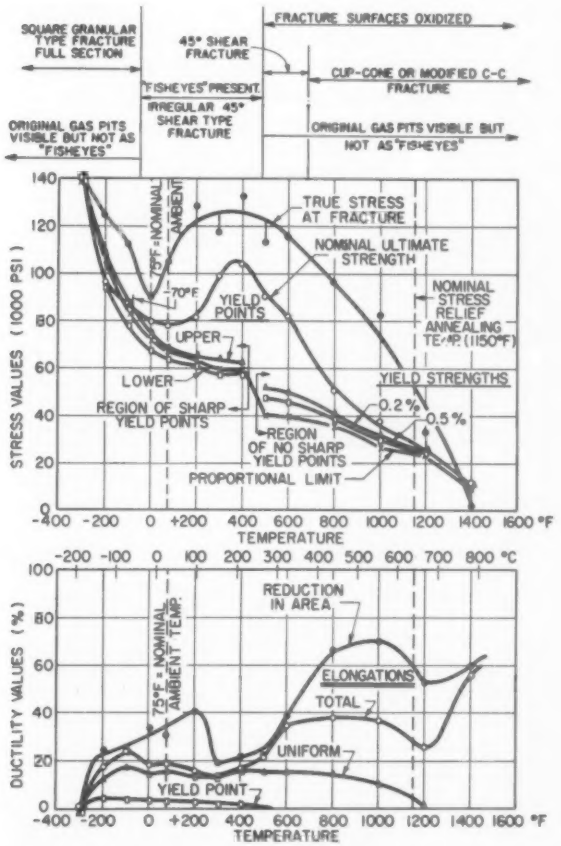


Fig. 8 Influence of temperature upon properties of manual, shielded metal-arc weld metal (covered electrode) in low-carbon steel deposit (C 0.05, Mn 0.31, Si 0.15, N 0.043) with 0.357-in. diam unnotched tensile specimen (metal as-deposited)

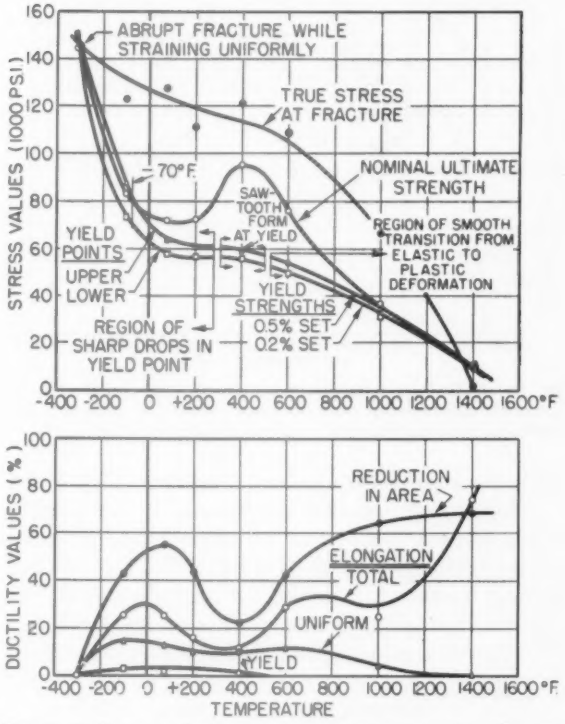


Fig. 9 Influence of temperature upon properties of manual, shielded metal-arc weld metal (covered electrode) in low-carbon steel deposit (C 0.05, Mn 0.31, Si 0.15, N 0.043) with 0.357-in. diam unnotched tensile specimen (metal stress relief annealed 1200° F)

a reversal from the comparison of nominal ultimate tensile strengths.

ELONGATION

Three measures of elongation were recorded: elongation at yield point; elongation at point of ultimate stress, which occurs before initiation of the localized reduction in cross section in the region of final fracture location; and elongation of the specimen in the 1.5-in. gage length at fracture. The first value, not usually reported, includes the irregular straining which precedes the uniform cold work elongation, lower Fig. 3. The second value is illustrated on the +200° F diagram of Fig. 4 and the third value is shown on the left of each of the diagrams of Figs. 3 to 5.

The relations of these three values to temperature are plotted on the lower graphs in Figs. 6 to 9. In a typical pattern, Fig. 7, elongation changes from a minimum at −300° F to an intermediate maximum somewhere between −100 and +100° F, reaches a secondary minimum between +200 and +400° F and then increases to as high as 102% total at +1400° F in the stress-relief annealed condition and 95% total in the as-deposited condition from the 0.019% N deposit.

The welds containing 0.043% nitrogen, and which also contained distributed gas voids, failed to achieve the high orders of elongation even at the elevated temperatures and significantly failed without measurable elongation at −300° F, both in the as-deposited

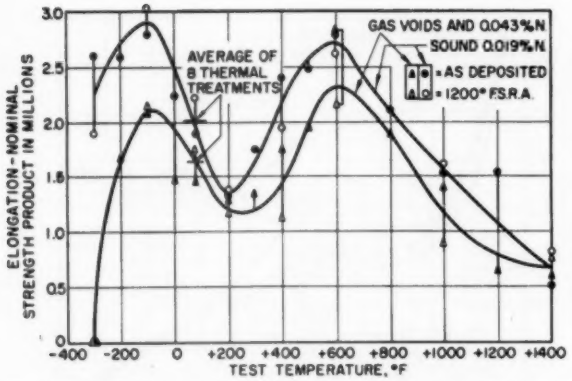

GAS VOIDS AND 0.043%N
SOUND 0.019%N
AVERAGE OF 8 THERMAL TREATMENTS
= AS DEPOSITED
= 1200° F S.R.A.
ELONGATION - NOMINAL STRENGTH PRODUCT IN MILLIONS
TEST TEMPERATURE, °F

Fig. 10 Relations between test temperature and area of rectangle containing tension load deflection diagram for covered electrode low-carbon steel metal-arc welds

and in the stress-relief annealed conditions, Figs. 8 and 9. The low-temperature brittleness in the 0.043% N deposit described here is not considered to be attributable to hydrogen in view of the fact that a +1200° F thermal treatment in argon for 1-hr on a 0.357-in. diam round will remove about 95% of the small quantity of hydrogen initially present within that period of time.

REDUCTION IN AREA

The percentage reduction in area at fracture for all welds bears roughly the same relation to temperature as the total elongation values, Figs. 6 to 9. Both criteria are therefore measures of ductility to about the same order of effectiveness: both are deleteriously influenced by the presence of internal gas voids and slag inclusions in the metal, by straining at the extremely low temperatures, and by gross straining within the +200 to +600° F temperature ranges.

WORK ABSORBING CAPACITY

The nominal ultimate tensile strength and the total elongation product, essentially the area of the rectangle containing the stress-strain curve to rupture, is sometimes taken as a criterion of the total work absorbed by the metal at rupture. A value of two million is considered to be a good one for this product for steel. The products obtained, for both as-deposited and stress-relief annealed weld metals, have maxima at −100° F and at +600° F with minima at or below −300° F, at +200° F and above +1400° F, Fig. 10. The low-nitrogen-content weld metal is characterized by a higher strength-elongation product than the higher nitrogen welds across the temperature range explored. This relative relationship of the two weld metals is also present in comparing the energy required to rupture V-notched Charpy impact specimens, Fig. 11, although there the transition from fibrous to granular fracture occurs at temperatures of about +40 to +60° F as contrasted to the −100° F and lower corresponding transition temperatures found from the unnotched tensile specimens under uniform stress. The impact data of Fig. 11 were secured from companion joints,

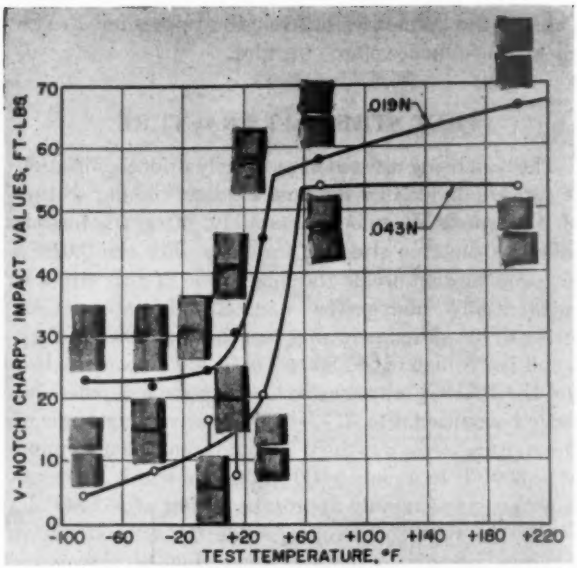


Fig. 11 Relations between temperature, Charpy V-notch values and nitrogen contents of covered electrode metal-arc steel weld deposits

welded to the same procedures. This difference in temperatures of transition from acceptably ductile to questionably brittle fracture is a warning that the term "transition temperature" has meaning only for a given metal composition and thermal treatment in a specific part or specimen geometry with a stated rate of loading. If all welds were made with polished surfaces, constant cross sections, and were slowly loaded in

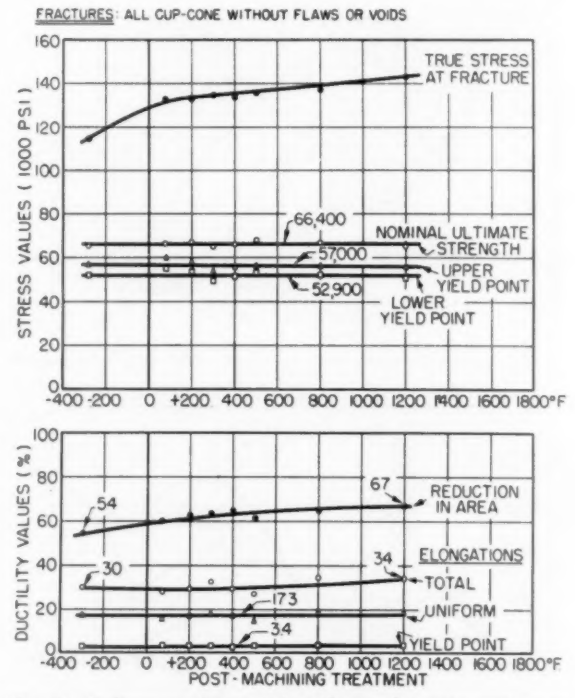


Fig. 12 Influence of pretesting thermal treatments upon +75° F tensile properties of sound manual covered-electrode shielded metal-arc weld metal deposits (C 0.06, Mn 0.32, Si 0.09 and N 0.019%), with 0.357-in. diam unnotched tensile specimens)

uniform tension there would be no industrial brittleness problems. Geometrical design of part and surface smoothness are obviously important features in welded performance for the best of analyses.

THERMAL TREATMENTS

The con used tensile properties of the 0.019% nitrogen we. .etals were little influenced by exposing them after finish machining to temperatures between −300 and +1200° F for 1 hr before testing at room temperature, Fig. 12. The true stress at fracture and the reduction in area, two interrelated values, were slightly improved by such prior treatment to increasingly higher temperatures. The other properties were not significantly altered, as shown by the low coefficients of variation in Table 5.

It is of particular interest that the usually determined tensile properties of weld metals were but little influenced by the +1200° F stress-relief annealing treatments of the small diameter bars of metal from which the surrounding base metal had been previously removed by machining. The elevated temperature data of Figs. 6 to 9 do show that while within the 1150 to 1200° F range, commonly used for stress relieving, the strength of the metal is reduced to the level where high stresses cannot be sustained at temperature.

During the initial cooling of the weld deposit it is presumed that the same stress and ductility interrelations with temperature are maintained as described

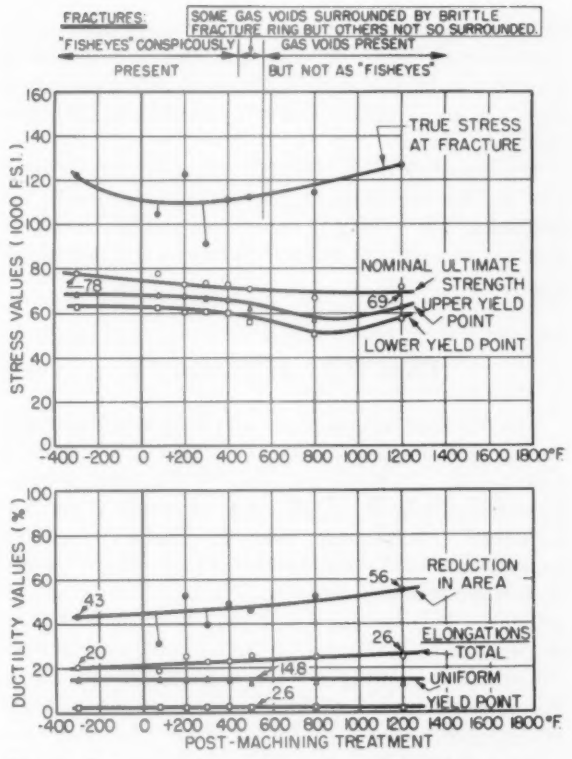


Fig. 13 Influence of pretesting thermal treatments upon +75° F tensile properties of unsound manual covered-electrode shielded metal-arc weld metal deposits (C 0.05, Mn 0.31, Si 0.15 and N 0.043%), with 0.357-in. diam unnotched tensile specimens

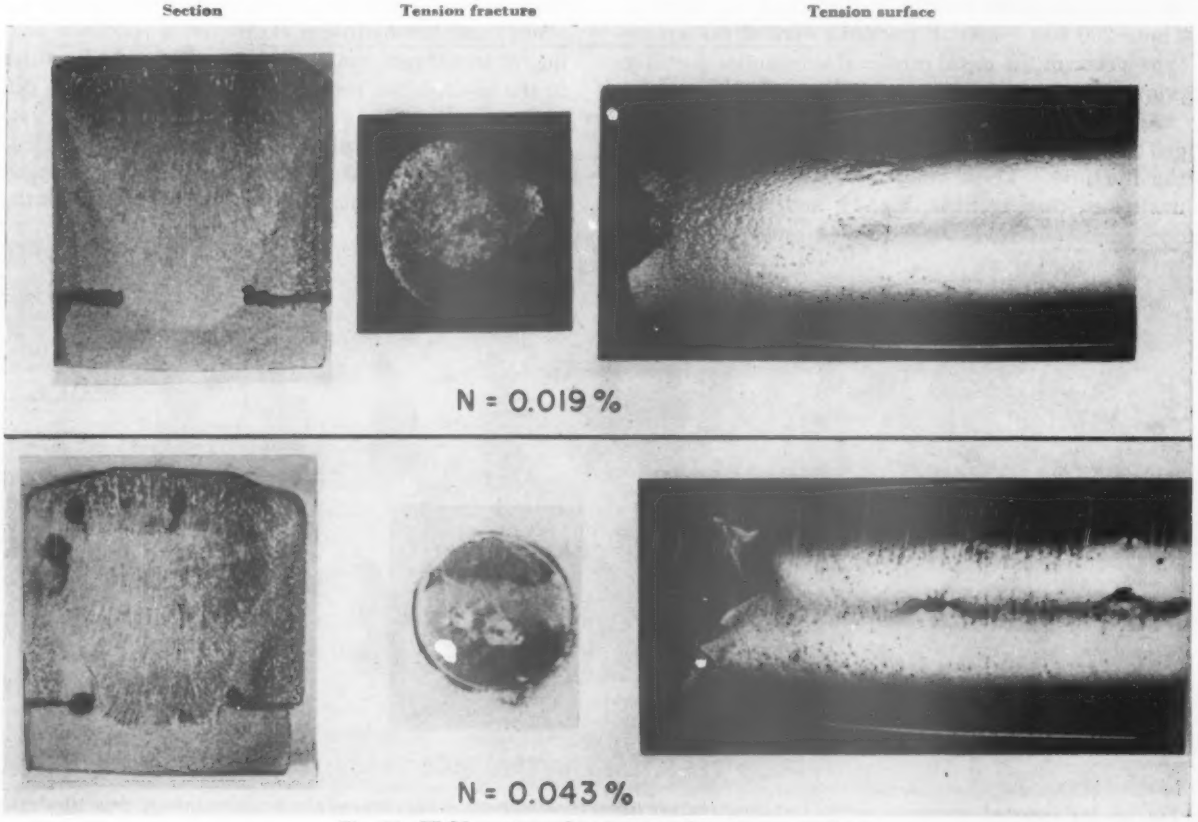


Fig. 14 Weld sections, fractures and specimen surfaces

herein and as shown in Figs. 6 to 9, although this is not a demonstrated fact. At any one time instant while making a welded joint all the stress-resisting potentials shown in those figures down to ambient temperature probably coexist.

The ductility of the 0.043% nitrogen content weld was slightly improved by progressive increases in the pretesting 1-hr baking temperatures and the strengths were correspondingly reduced slightly by this procedure, Fig. 13 and Table 5.

FRACTURE APPEARANCE

The fracture appearances of the 0.043% nitrogen welds were strikingly altered by the prior thermal treatments. All room temperature fractures in specimens baked 1-hr at temperatures of +400° F and less contained conspicuous fisheyes. Those specimens treated to +600° F and above for 1-hr did not contain such characteristics although the gas voids were always present. This corresponds to prior findings for hydrogen and indicates that similar characteristics result from the presence of nitrogen. The solubility of nitrogen in iron is temperature variable in a manner similar to that of hydrogen, although the dissolved gas is far less mobile in the solid metal.

For the temperatures above −200° F all but two fractures of the lower nitrogen weld deposits were of the cup-cone or modified cup type and all of these welds were free of visible internal gaseous and slag defects. The −200 and −300° F fractures were of the square type although the metal exhibited substantial ductility prior to fracture; not less than 17% at −300° F.

All fractures of welds containing 0.043% fixed nitrogen contained evidences of small to minute gas voids in the fractures. These voids were also visible on the unstressed cross sections, Fig. 14, and resulted in the common "fisheye" type regions on the fractures when strained to rupture at the test temperatures between 0 and +400° F. At −100° F and below, "fisheyes" were not apparent in the fractures, because at those temperatures the entire fracture surface was of the "granular" type. A "fisheye" is therefore a gas or slag nucleus surrounded by a granular type of fracture which is in turn surrounded by a fibrous type of failure, Fig. 15. It necessarily loses its identity when the entire area of metal surrounding the nucleus assumes the brittlelike granular type of fracture. Conversely, at temperatures of +500° F and above where the brittlelike type fracture does not occur, even in the immediate region surrounding the stress-raising gas void, the typical "fisheye" structure no longer appears. One may speculate that this change is at least partially related to the local relief in stress, aided by the shift from sharp to gradual yield stress characteristic with its resultant change in void periphery stress distribution pattern. There is a strong indication here tending to refute the commonly held concept that "fisheyes" are caused only by internal pressure of hydrogen. This does not imply that in other welds hydrogen is not a factor in their appearance. It is probable that several gases, including hydrogen, nitrogen, carbon monoxide and carbon dioxide, are sometimes responsible as the nuclei for this phenomenon. Supporting evidence is that average number of "fisheyes" per fracture can be related to weld nitrogen content and that a high incidence of "fisheyes" per fracture can be produced in welds made with a long arc when using low-hydrogen electrodes, a condition conducive to nitrogen contamination. The weld fractures in the as-deposited condition across the test range can be seen in Fig. 15.

For unnotched tensile specimens it can be stated that the transition in tension fracture appearance changed from fibrous to granular between 0 and −100° F for the

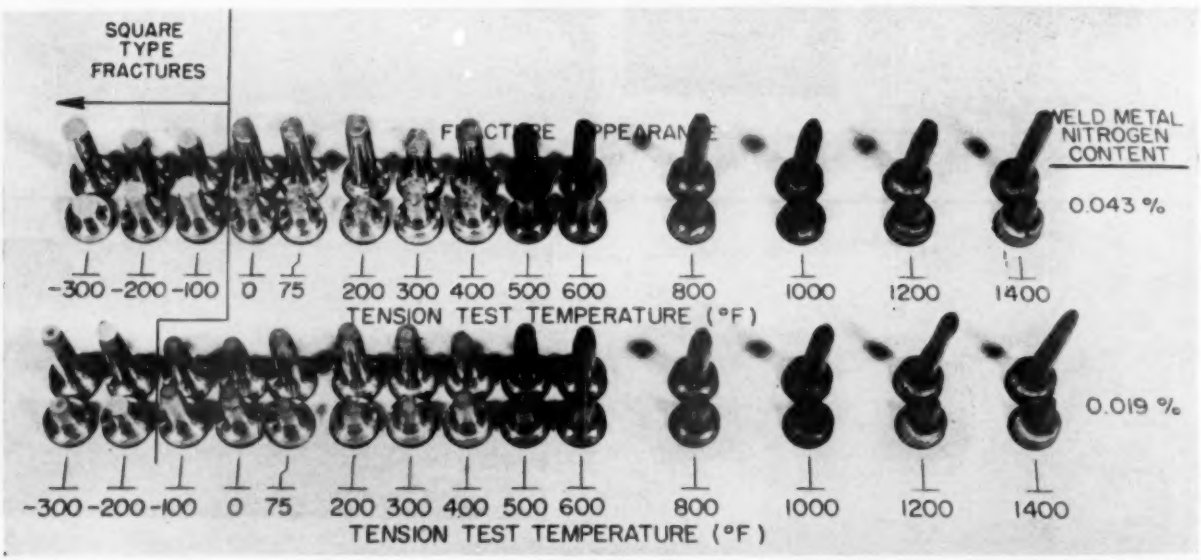

SQUARE TYPE FRACTURES
FRACTURE APPEARANCE
WELD METAL NITROGEN CONTENT
0.043 %
0.019 %
-300 -200 -100 0 75 200 300 400 500 600 800 1000 1200 1400
TENSION TEST TEMPERATURE (°F)
-300 -200 -100 0 75 200 300 400 500 600 800 1000 1200 1400
TENSION TEST TEMPERATURE (°F)

Fig. 15 Influence of composition and test temperature upon tensile fractures of covered electrode metal-arc deposited steel weld metal

0.043% N welds and between −100 and −200° F for the 0.019% N deposits.

STRAIN AGING

The jagged irregularity of the plastic strain region of the +300° F test load-strain diagrams illustrated by Fig. 4 is a metal and not a machine characteristic. This phenomena occurred at the +300 to +400° F temperatures for welds deposited by both electrodes and in both the as-deposited and stress-relief annealed conditions. It is a manifestation of the strain aging phenomena which is related to the precipitation of iron nitrides under the influence of temperature and stress.

SUMMARY

Tensile properties of covered electrode medium and high-nitrogen-content weld metals in uniformly loaded specimens have been determined across the temperature range from −300 to +1400° F in both the as-deposited and stress-relief annealed conditions. While the general property-temperature trends are similar to those long established for wrought steel, it has been demonstrated that nitrogen content, a criterion of arc shielding efficiency, has a significant affect upon weld ductility and work absorbing characteristics across the entire temperature range. Even under uniform stress in an unnotched tension specimen, zero ductility is approached at −300° F in the higher nitrogen content welds. This characteristic is not improved by a subsequent +1200° F anneal. Complete brittleness can be secured in the absence of hydrogen.

Below −75° F the upper yield point stress exceeds that of the nominal ultimate tensile stress as commonly defined. In the higher nitrogen deposits the upper yield point stress at −300° F equals the true stress at fracture and completely brittle failure is therefore achieved.

At and above +500° F the characteristic of a sharp upper yield point stress disappears and yield stress can then be described only by arbitrary assignment of permanent set values. At +300° F the phenomena of strain aging is exhibited in both of the weld metals tested.

Stress relief annealing at +1200° F has little beneficial effect upon the properties of the weld metal, as such, but treatment at that temperature assures the removal of high orders of imposed shrinkage stresses.

The presence of higher nitrogen contents effectively increases the yield and nominal ultimate tensile stresses but reduces the true stress at fracture, the ductility values and the work absorbing capacity. It is also responsible for the presence of gas voids which may form the nuclei for the commonly observed "fisheyes" in weld fractures. Nitrogen contents as high as 0.020% are not detrimental to the ordinary tensile properties of arc-deposited steel weld metal, although lower amounts are indicated to be beneficial in impact.

The Spot Welding of Titanium

Physical properties of titanium spot welds using different settings and thicknesses of material

By E. R. Funk

INTRODUCTION

THE joining of fabricated sheet metal parts is an important aspect of metal fabricating. Titanium can be joined by the usually recognized processes with some special attention to the problem of gas adsorption elevated temperatures. The joining processes in use at Goodyear Aircraft include spot welding, fusion welding, and the conventional mechanical process of riveting and bolting.

At Goodyear Aircraft we have been successful in working out procedures which gave satisfactory results. It is the purpose of this paper to present to the general fund of knowledge those things of interest that we have learned about joining titanium in the past several years.

E. R. Funk is Engineering Consultant, Technical Service Department, Goodyear Aircraft Corp., Akron, Ohio.

EXPERIMENTAL CONDITIONS

The spot welding of commercially pure titanium has been the subject of considerable research in various laboratories of this country. Our present concern is to present data and experience accumulated in this organization.

An effort was made to accumulate some data that would be of interest to both the designers and the production and quality control groups. This meant that a study of the physical properties of titanium spot welds should be undertaken using different settings and thicknesses of material.

The material used in the tests was commercially pure titanium Ti-75A produced by the Titanium Metals Corp.

Table 1—Spot Welding of Ti-75A, 0.020-Gage Sheet, Pressure 30 Psi, Electrode Force 1250 Lb

Spec. No.	*Cycles*	*Current, amp*	*Shear, lb*	*Tension, lb*	*Penetration, %*	*Diameter, in.*
2-46	3	7,200	820	26	60	0.183
2-56	3	8,600	1090	180	67	0.205
2-66	3	10,200	1145	222	73	0.219
2-76	3	11,700	1265	200	80	0.230
2-86	3	13,200	1180	155	85	0.240
4-46	6	7,200	1055	13	70	0.201
4-56	6	8,600	1130	225	76	0.223
4-66	6	10,200	1240	235	90	0.230
4-76	6	11,700	1240	140	82	0.250
6-46	9	7,200	1045	233	..	0.209
6-56	9	8,600	1155	165	82	0.230
6-66	9	10,200	1190	172	89	0.248
6-76	9	11,700	1195	125	90	0.257
8-46	12	7,200	1015	205	78	0.220
8-56	12	8,600	1145	210	87	0.229
8-66	12	10,200	1300	155	95	0.250
8-76	12	11,700	1265	116	95	0.272
12-36	18	5,500	660	122	60	0.195
12-46	18	7,200	1050	195	80	0.223
12-56	18	8,600	1190	...	90	...
16-36	24	5,500	730	54	63	0.195
16-46	24	7,200	1015	130	82	0.208
16-56	24	8,600	1225	208	94	0.235
20-36	31	5,500	730	45	65	0.195
20-46	31	7,200	1095	180	..	0.216
20-56	31	8,600	1155	205	95	0.255
30-36	50	5,500	705	46	70	0.205
30-46	50	7,200	1130	243	85	0.235
30-56	50	8,600	1220	...	95	...

of America. The sheet used was 0.020 in. thick and received in the annealed condition.

All the titanium spot welding carried out at the Goodyear Aircraft plant was done on a Taylor-Winfield machine, Type ERB-24-125, having a rated capacity of 125 kva. A thyration synchronous control and sequence panel was used on this machine. The values of current reported are calculated from the primary current. A variety of electrodes have been used in our shop for titanium, but the data reported here was taken using a Mallory No. 3 alloy straight shank, $2^1/_2$-in. long electrode, with a 3-in. radius tip.

Specimens were cut from the sheet stock and were $^3/_4$ in. wide and 3 in. long. These strips were degreased in the vapor degreaser and wire brushed with a stiff rotating wheel of stainless steel wires. The resulting surface resistance was 35 to 40 micro-ohms. An electrode force of 1250 lb. and 60-cycle squeeze time were used throughout.

Sample spot welds were prepared at varying currents and cycles so that a wide range of each variable was investigated. Tensile shear and direct tension data were taken under these varying settings. The data are presented in Table 1.

EFFECTS OF WELD CURRENT

In spot welding, a major variable is the weld current. In these experiments the weld current was controlled by phase shifting. The known primary current was multiplied by the percent phase shift to give the current flowing through the secondary of the machine and thus the current to produce the spot weld.

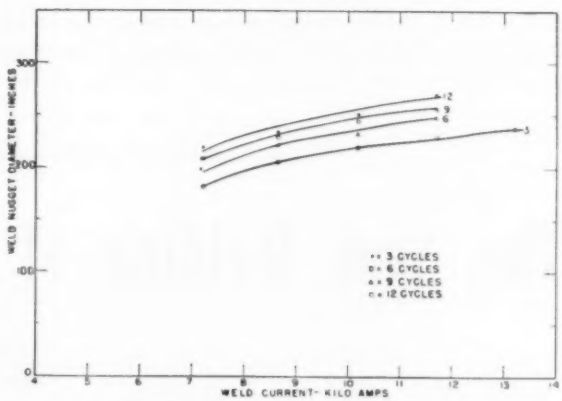


Fig. 1 Weld nugget diameter for Ti-75A titanium sheet of 0.020-in. gage vs. weld current for various cycles

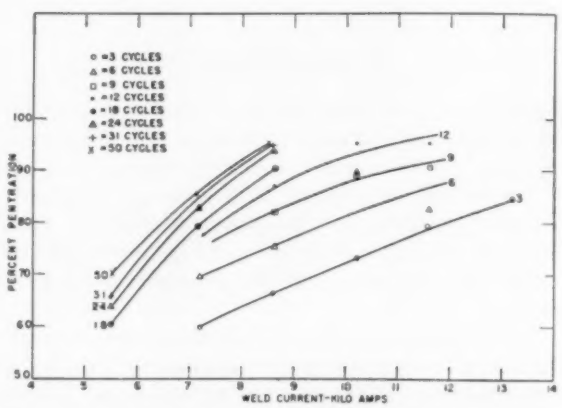


Fig. 2. Percent penetration for Ti-75A titanium sheet of 0.020 gage vs. weld current for various cycles

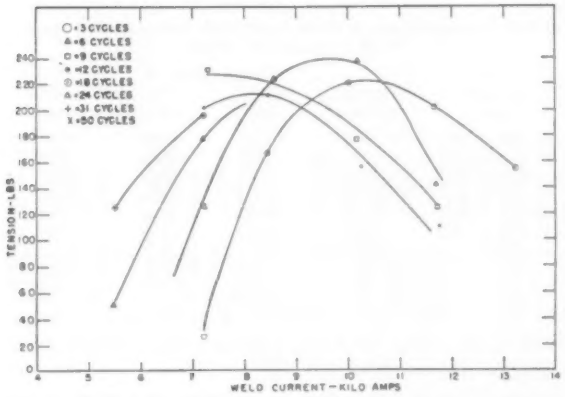


Fig. 3 Tension vs. current. Ti-75A titanium 0.020-gage spot weld at various cycles

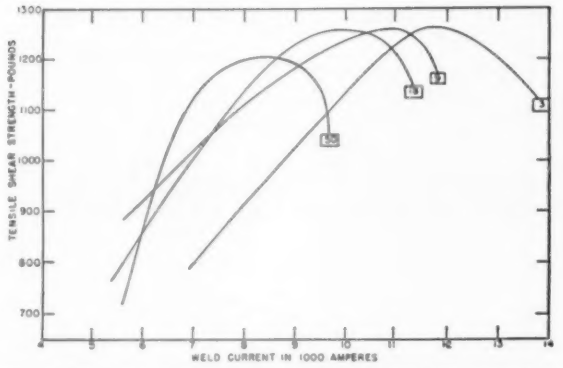


Fig. 5 Tensile shear strength vs. weld current at various weld cycles for Ti-75A titanium sheet

The effects of spot weld current on the size and shape of the weld nugget are shown in Figs. 1 and 2. It can be seen that the weld diameter as well as penetration increase with the increase of current. The penetration approaches 100% of the indented weld thickness at higher currents. Of course this result is expected since the nugget size depends on the rate of heat input and the rate heat dissipation. Titanium, a metal of high electrical resistivity and low heat conductivity, can be expected to show high penetration.

The large penetration results in a decrease of the tension strength of a nugget as shown in Fig. 3 where the tension strength is plotted against weld current. The device for tension testing is shown in Fig. 4. The tension values reported must be considered only relative as the tension values are known to be a function of the type of loading the tension jig imposes on the nugget. In the experiments reported here, it should be recognized that the jig imposed a considerable degree of bending in the specimen strips. This resulted in stress concentrations at the edge of the nugget. In all the tension failure the nugget pulled out of one sheet except at very low heats where the sheets separated.

The large effect of the stress concentration in reducing the tension values was demonstrated conclusively by a simple experiment. Several spots were made at a medium heat and time (8600 amp, 8 cycles) and the specimens sheared to 1- x 1-in. squares. The spot weld nugget was centrally located in the square. Each specimen was then joined by Bondolite resin to two 1-in. aluminum cubes. In this way the spot weld nugget which was now between the aluminum blocks could be loaded in direct and uniform tension. A tensile test was performed. At 1600-lb load, the resin pulled loose leaving the nugget still unbroken. This experiment proved that the pure tensile properties of a nugget are at least six and a half times the values found when some bending of the strip is permitted.

These results mean that such design criteria as tension/shear ratios are very dependent on the type of jig used. In actual service the resistance of a spot to tension failure depends on the stiffness of the section and the constraint imposed by adjacent spots and the surrounding structure.

The tensile shear tests were performed on specimens $^3/_4$ in. wide with a single spot and pulled on a Baldwin-Southwark tensile machine. The effect of weld current on tensile shear values is shown in Fig. 5. It can be seen that these values approach a maximum as the current

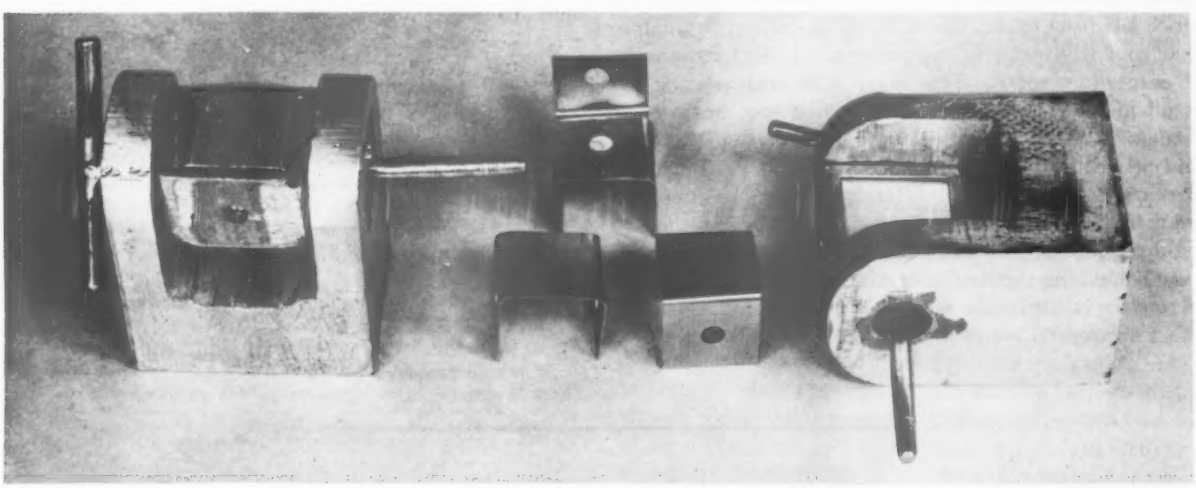


Fig. 4 Device for tension testing spot welds

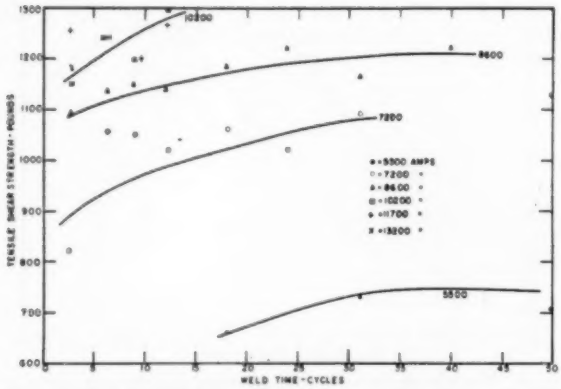


Fig. 6 Tensile shear strength vs. weld time. Ti-75A titanium sheet at various currents

increases and then begin to fall off. At the high current settings flashing and electrode indentation became excessive.

Although the optimum tensile shear values are 10,200 amp and 12 cycles, it can be seen that a wide range of current settings and time give satisfactory shear values for titanium. This conclusion has been reached by all investigators working in this field.

EFFECT OF WELDING TIME

The effect of the number of cycles of current flow of the penetration of the nugget is clear in Fig. 2. Of course the limit of penetration is 100% and this limit is approached faster at greater cycle times and higher current settings. It can be seen that a wide variation of time of current flow may be permitted and result in constant penetration. The variation of the tensile shear strength with weld cycle time is shown in Fig. 6. Although there is a considerable scatter in the data, the general relation of time, current and strength is clear.

EFFECT OF MULTIPLE SPOTS

The results of the tests reported above were for single spots. It is well known that after the first spot of a series has been made, the shunting of current through that spot will result in less current for the formation of succeeding spots. This may, with some settings, result in subquality spots after the first spot has been made. To study this effect, wide sheets were prepared and cleaned by wire brushing and a series of welds made at various settings. The standard shear specimens were then cut from the plate. The results of these tests are reported in Table 2.

In comparing the results of multiple spots with those of single spots, it can be seen that in titanium the shunt effect apparently is not a factor since in no case is the first spot a "hot" spot. In fact, the first spot averages about 2.5% lower than the average of the other spots in tensile-shear values.

Table 2—Comparison of First Spot with Subsequent Spots, Ti-75A Titanium Sheet

Gage combinations	*Tensile shear, lb*			
	Average of all spots	*First spot*	*High value*	*Last spot*
0.025–0.032	1327	1300	1380	1250
0.025–0.025	1211	1200	1240	1200
0.025–0.032	1389	1340	1420	1460
0.025–0.025	1215	1180	1250	1190
0.025–0.025	1147	1130	1190	1170
0.032–0.032	1806	1740	1900	1700

These data indicate that in titanium the shunt effect is not found. Apparently the electrical resistivity is sufficiently high for titanium so that little current shunts through neighboring spots. This contrasts to high conductivity aluminum where the first spot is always a "hot" spot and succeeding spots are smaller due to the flow of current through the neighboring spots.

An explanation for the fact that the first spot averages 2.5% lower than the others probably lies in the better positioning and alignment of the sheet once a spot is made than when the sheets are manually held. Actually in spot welding quality control, a 2.5% deviation is seldom significant.

RECOMMENDED TITANIUM SPOT WELDING SETTING AND CONCLUSIONS

Strong and satisfactory welds can be made within the following range of conditions for Ti-75A of 0.020 in. thickness:

Weld current—7000 to 12,000 amp

Weld time—6 to 15 cycles

Above 10 cycles there is little change in penetration with weld time since the penetration approaches 100% at this value. The nugget diameter continues to increase with increased time.

Several experiments have been made on electrode contour from time to time. No change in tensile shear values results from tip radius changes in the range from 3 to 10 in. Also the weld force has been varied from 1000 to 1700 lb and found to have very little effect on strength.

The work reported in this paper confirms that of other investigators:[1, 2] titanium can be spot welded satisfactorily within a wide range of conditions.

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Alloy Welds Deposited in "Unalloyed" Titanium Base Metal: Part II

An investigation to develop a method for improving the toughness and strength of weld joints in "unalloyed" titanium

by C. E. Hartbower and Daniel M. Daley, Jr.

Summary—Part I

A number of commercially available materials have been investigated as to their usefulness for fabrication by fusion welding of end items requiring a minimum proportional limit of 50,000 psi. Weldability was evaluated on the basis of tensile tests of longitudinal and transverse welds in 1/16-in. sheet, Vickers hardness surveys of the weld joints, and guided bend tests at 0 and −40° C. Throughout the investigation, the total interstitial content, *C*arbon, *O*xygen, *H*ydrogen and *N*itrogen, referred to hereinafter as the COHN, was evaluated as to its effect on various mechanical properties.

The tensile test demonstrated the increase in base-metal strength that can be realized by the combined presence of oxygen and nitrogen. An increase from 0.167% oxygen and 0.040% nitrogen to 0.238% oxygen and 0.089% nitrogen raised the proportional limit from 55,000 to 78,200 psi, an increase of 42%. The tensile elongation attending these strengths in the "unalloyed" base metals was adequate (approximately 23% in 2 in.). Tensile tests of weld joints oriented transverse to the direction of tensile loading were found to be of little use in determining the strength of weld joint because deformation and fracture occurred in the base metal in such a specimen (unless there was an extreme embrittlement of weld or heat-affected zone). However, with the weld joint longitudinal to the direction of tensile loading, deformation was forced to occur equally in all components of the weld joint. When a low COHN "unalloyed" base metal was welded with various alloy filler metals, weld joint tensile elongations of 10 to 12% were attended by proportional limits of between 73,350 and 80,100 psi and ultimate tensile strengths of between 107,600 and 118,500 psi.

Chemical analyses were made of all filler and base metals as well as most of the weld deposits. The interstitial content of both filler and base metal was found to have a marked influence on the COHN of the deposited weld metal. Hydrogen was an exception in that the hydrogen content of the weld was found to be lower than what was expected from a consideration of the filler and base metal hydrogen content. The oxygen in the alloy weld deposits in base metals A, B and C ranged between 0.125 and 0.168 wt. % (average 0.148%); the hydrogen, between 0.0055 and 0.017% (average 0.011%); and the nitrogen, between 0.028 and 0.072% (average 0.047%). The carbon in the case of base metals A and B ranged between 0.05 and 0.14% (average 0.09%). The only distinguishing feature of welds in base metal C was the carbon content which ranged between 0.13 and 0.15% (average 0.14%); otherwise, the composition of the welds in base metal C was similar to that of the welds in base metals A and B. The average oxygen content of the welds in base metal C was 0.152%, the average hydrogen 0.014% and the average nitrogen 0.050%. The alloy content (beta-stabilizing elements) of the weld deposits was considerably reduced from that of the commercial alloys used as filler metal. The lowering of the alloy content was attributed to dilution by arc-melting of the "unalloyed" base metal and to loss by vaporization of the more volatile elements.

Weld hardnesses of 216 to 245 Vhn were developed by depositing "unalloyed" filler in "unalloyed" plate, representing an average increase in hardness of 56 Vhn over that of base metal A, 25 Vhn over base metal B and 3 Vhn over that of base metal C. The alloy fillers deposited in "unalloyed" base metal developed hardnesses of 215 to 299 Vhn, representing an average increase in hardness of 74 Vhn over that of base metal A, 89 Vhn over base metal B and 52 Vhn over base metal C. Hardness correlated with COHN, providing the carbon content was low (less than 0.10%). The latter confirms the observation of other investigators; viz., that carbon is a less effective hardener than nitrogen or oxygen.

Guided-bend tests involving a 1T bend radius showed very little difference between the various fillers deposited in a given base metal (base metals A and B were evaluated). "Unalloyed" fillers and alloyed fillers all produced weld deposits which were ductile in the bend test at a testing temperature of 0° C (approximately 30% elongation). At −40°, however, the welds in the base metal of higher interstitial content tended to develop cracks but only after appreciable bending (50–60 deg of bend angle). Thus, the bend test showed a loss of weld ductility as the result of a higher base metal COHN, but not a severe embrittlement.

Part II

Notched-Bar Impact Testing

NOTCH toughness of the deposited weld metal produced by the various weld and base metal combinations was determined using a modified V-notched Charpy impact bar (Fig. 20). The specimen is double the width (0.788 in.) and half the thickness (0.197 in.) of a standard Charpy bar, thus making the specimen useful for evaluating material thicknesses between 1/4 and 1/2-in. The notch is the same as in a standard Charpy except for depth (0.039 in.). For a comparison of performance between the standard and modified speci-

C. E. Hartbower is Chief and **Daniel M. Daley, Jr.**, is Welding Engineer, Welding Section, Process Metallurgy Branch, Watertown Arsenal Laboratory, Watertown, Mass.

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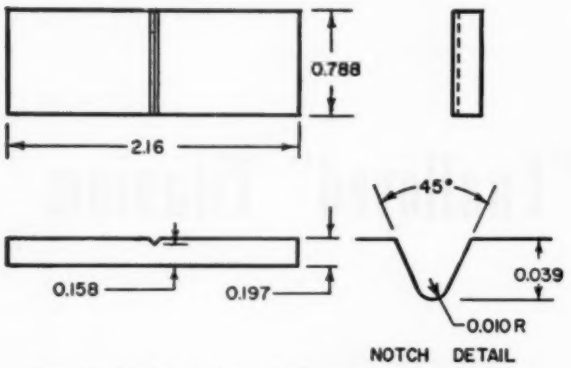


Fig. 20 Modified V-notch Charpy Impact specimen

The notched bar is twice the width and half the thickness of a standard specimen. Modification of the Charpy specimen permitted testing plate thicknesses in the range of 1/4 to 1/2 in.

mens, 1/2-in. thick "unalloyed" titanium from two commercial sources was machined into standard and modified specimens and tested over a range of temperature in a standard impact-testing machine (Fig. 21). The maximum energy level for the modified Charpy bar was appreciably less than the standard Charpy (in the case of "unalloyed" Titanium B, laminations caused an even greater difference in maximum energy level). At low energy levels, however, data from the two types of specimens, to all practical purposes, coincided. Thus, comparisons based on the temperature corresponding to 10 ft-lb, for example, can be considered to quantitatively apply to both the modified and standard Charpy impact specimens.

The notch toughness of the three 1/4-in. "unalloyed" base metals investigated (Fig. 22) was poor in comparison to the toughness obtainable in high-purity "unalloyed" titanium (see Appendix). Two orientations of impact specimen were tested (longitudinal and transverse to the long dimension of the 1/4-in. plate). The data so obtained produced one curve (plotting temperature versus impact energy) with remarkably little scatter. Attempts to correlate the performance with interstitial content indicated that the displacement of the three curves relative to one another correlated with the total gas content but not with the total COHN.

Investigators at Battelle Memorial Institute[6] found that the impact-energy values of high purity iodide and commercial-purity titanium were markedly decreased by relatively small additions of hydrogen and that the effect could be eliminated by removing hydrogen by vacuum annealing. For example, 0.020 weight percent hydrogen in commercial-purity titanium lowered the impact energy at −40° C from 38 in.-lb in vacuum annealed material to 6 in.-lb (micro-impact). Consider-

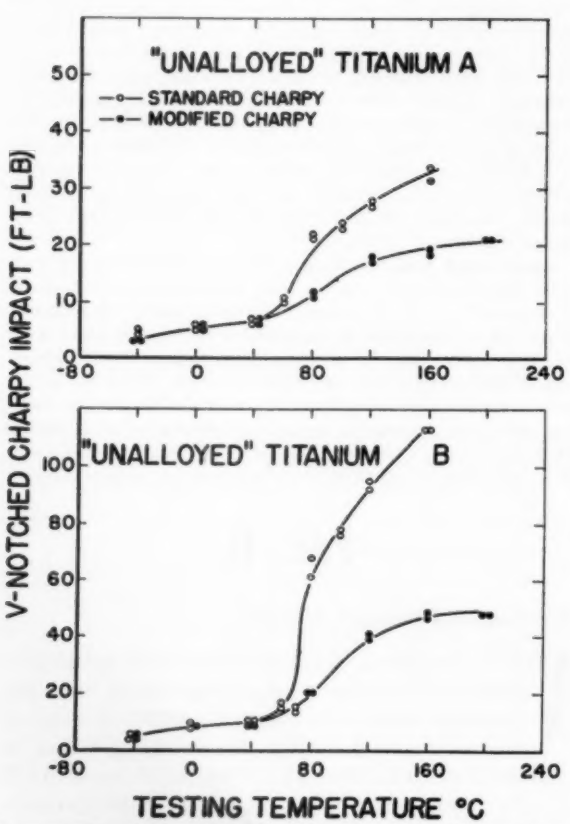


Fig. 21 Correlation of standard and modified Charpy bars

A 1:1 correlation is indicated at low energy levels; thus, comparisons based on the temperature corresponding to 10 ft-lb should be significant to both specimens. One heat of alloy titanium (RC-130AW) has been tested (data not shown) and the same correlation was found to hold with remarkably little scatter in the test results.

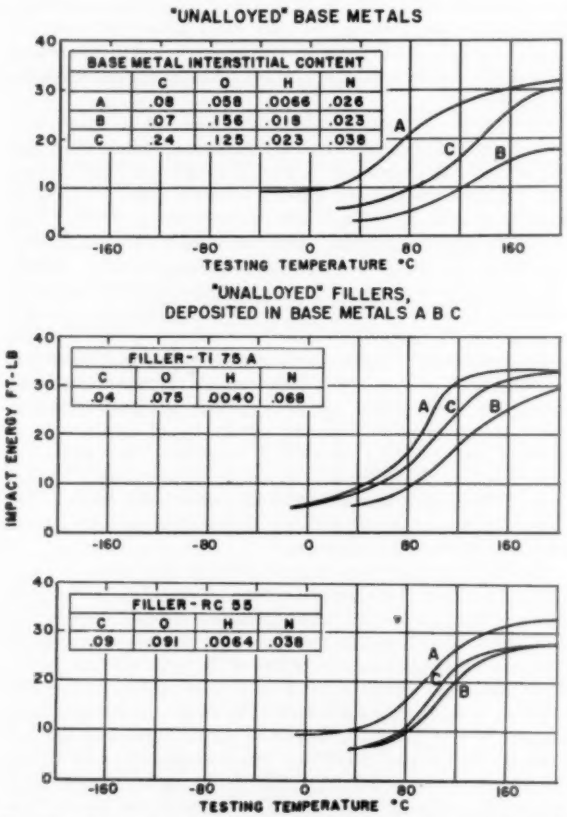

"UNALLOYED" BASE METALS
BASE METAL INTERSTITIAL CONTENT
C O H N
A .08 .058 .0066 .026
B .07 .156 .018 .023
C .24 .125 .023 .038
TESTING TEMPERATURE °C
"UNALLOYED" FILLERS,
DEPOSITED IN BASE METALS A B C
IMPACT ENERGY FT-LB
FILLER - TI 75 A
C O H N
.04 .075 .0040 .068
FILLER - RC 55
C O H N
.09 .091 .0064 .038
TESTING TEMPERATURE °C

Fig. 22 Notch toughness of "unalloyed" base metals and weld deposits

Transition curves obtained with the modified Charpy bar showed the base metals to be notch sensitive. Complete curves were obtained for the base metals tested in two orientations of specimen; remarkably little scatter resulted when the data of the two orientations were superimposed. Note that the notch-bar impact properties of the "unalloyed" weld deposits were very nearly the same as for the "unalloyed" base metals.

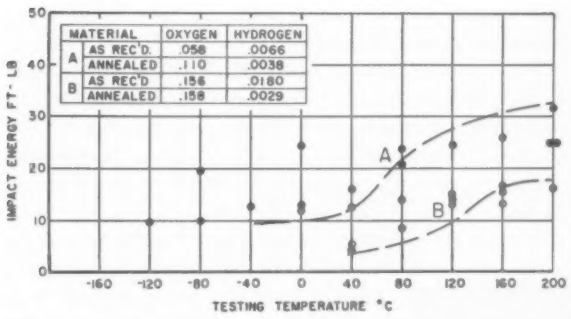


Fig. 23 Notch toughness of "unalloyed" base metals A and B vacuum annealed

Dashed curves represent the as-received base metals. After vacuum annealing, the scatter was markedly increased with an indication of improved toughness at low-temperature levels.

able improvement in toughness could also be obtained at the lower hydrogen concentrations by quenching from the alpha-phase field. However, the Battelle investigators found that the toughness so obtained decreased with time at room temperature, indicating an agglomeration of the hydride phase.

To determine the effect of a vacuum anneal on the transition curves of base metals A (0.0066% H) and B (0.018% H), 30 finish-machined impact specimens were vacuum annealed for $1^1/_2$ hr at 1550° F at a pressure of 1 to 3 microns.* The pieces were then cooled in the vacuum retort to a temperature at which they could be handled by hand. An improvement in the low-temperature range of the transition curves was indicated but the scatter in test results was too great to permit any conclusions (Fig. 23). Chemical analysis of two specimens from each of the impact-energy curves for the vacuum-annealed base metals provided the data of Table 16. Thus, vacuum-annealing lowered the hydrogen content appreciably but the amount was not consistent. The increase in oxygen content in base metal A after vacuum annealing as compared with the relatively constant oxygen content in the case of base metal B suggests that the pickup of oxygen may be more rapid in materials initially low in interstitial content.

A comparison of vacuum-annealed base metal A Sample 2 and base metal B Sample 1 shows that oxygen is a major factor in controlling notch toughness. However, the correlation of hydrogen content in the individual samples and the impact-energy values is also noteworthy. Vacuum annealing of welds has not been attempted as yet, but is considered a very important phase of future work.

The notch toughness of the "unalloyed" as-deposited welds in the three "unalloyed" base metals was to all practical purposes the same as that of the unwelded base metals. The "unalloyed" welds in base metal C with its intermediate oxygen and higher carbon, hydrogen and nitrogen contents maintained the same intermediate position relative to base metals A and B as in the unwelded condition.

In addition to the more or less abrupt lowering of energy values over a narrow temperature range, a transition in fracture appearance was observed in the specimens taken from the "unalloyed" welds. The transition in fracture-appearance occurred at approximately the same temperature range in base metals B and C, irrespective of which "unalloyed" filler was used; viz., between 80 and 120° C. The transition in

* The vacuum anneal was conducted by Hugh W. Cooper through the Superior Tube Co., Norristown, Pa.

Table 16—Effect of Vacuum Annealing

	As-received	*Vacuum annealed—Sample 1*	*Vacuum annealed—Sample 2*
	Base metal A		
Hydrogen, %	0.0066	0.0042	0.0034
Oxygen, %	0.058	0.112	0.109
Impact, ft-lb	10	13	19
	Base metal B		
Hydrogen, %	0.0180	0.0035	0.0023
Oxygen, %	0.156	0.160	0.157
Impact, ft-lb	4	5	12

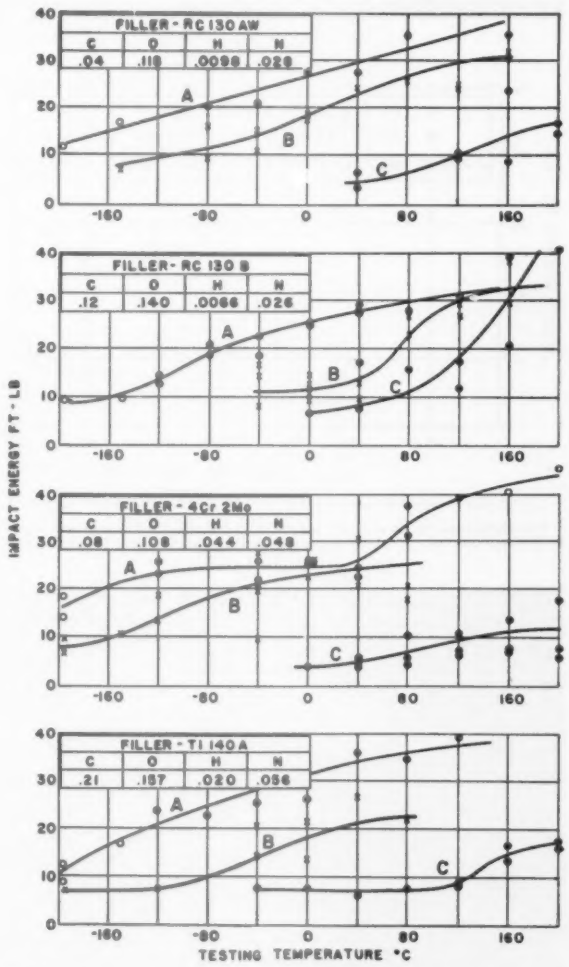


Fig. 24 Notch toughness of alloy fillers deposited in "unalloyed" base metals A, B, C

A comparison with Fig. 23 shows the marked improvement in toughness that is realized by the use of alloy fillers when welding base metals A and B. Alpha-beta welds in base metal C on the other hand, in three out of four cases were even more notch sensitive than the weld deposits produced with "unalloyed" filler metal. The tabulated interstitial content is for the filler metals *before* deposition.

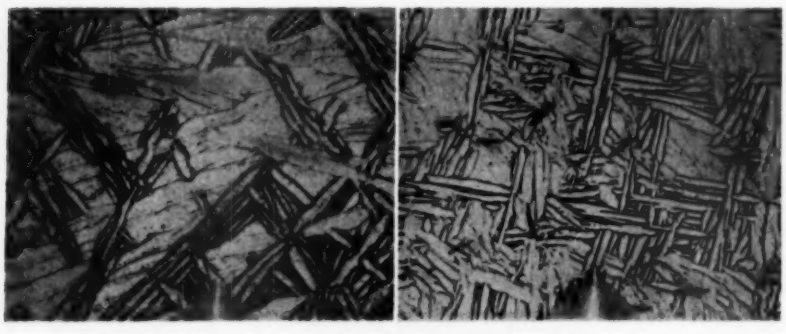
Table 17—10 Ft-Lb Transition Temperature ° C Synopsis

	Base metal		
Filler	*A*	*B*	*C*
RC-55	40	80	80
Ti-75A	40	60	80
RC-130B	−150	0	60
Ti-140A	−200	− 80	130
RC-130AW	−200	−100	120
4Cr - 2Mo	−200	−160	140

the fracture appearance of welds in base metal A occurred in the range of 40 to 80° C.

The consistently poor performance of the "unalloyed" welds was noteworthy particularly in light of the low interstitial content of the two filler materials used. The marked influence of the interstitial content of the base metals on the toughness of the weld deposits is apparent.

The notch toughness of the alloyed weld deposits in "unalloyed" base metals A and B was greatly improved as compared with the all-alpha "unalloyed" weld deposits (Fig. 24). In marked contrast to the performance of the alloy welds in base metals A and B, the alloy fillers deposited in base metal C were more notch sensitive than the "unalloyed" fillers deposited in base metal C (with the notable exception of RC-130B). Table 17 summarizes the testing temperatures corresponding to 10 ft-lb of impact energy. The transition from high to low energy values in the case of the alloy weld deposits was spread over a broad temperature range (again with the notable exception of RC-130B in base metal C).* Likewise, the transition in fracture appearance was too gradual to be a useful criterion of performance.



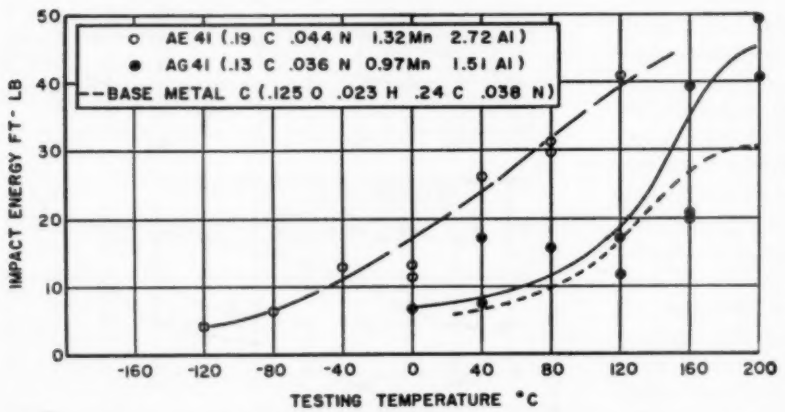


Fig. 25 Anomalous behavior of RC-130B filler deposited in base metal C

The difference in chemical composition between AE41 and AG41 is attributed to a difference in joint preparation. × 1000 (area approximately 3 x 4 in. before reproduction).

* The exceptional performance of RC-130B deposited in base metal C was considered worthy of more detailed examination. The first data obtained were inconsistent with all other alloy welds in base metal C (see dash curve (AE41) of Fig 25). Consequently, additional data were obtained which proved to be somewhat more consistent with the trends indicated by the other fillers (AG41). However, the data still appeared anomalous in that the transition from high- to low-impact energy was abrupt and the 10 ft-lb transition was much lower than for any of the other alloy weld deposits in base metal C. Microexamination revealed a coarser structure in the more notch tough weld deposit but otherwise the structures were the same. The hardness of the first and second pass weld metal were higher in the more notch tough weld deposit (270–293 Vhn) than in either the check weld (254–278 Vhn) or the base metal (220 Vhn).

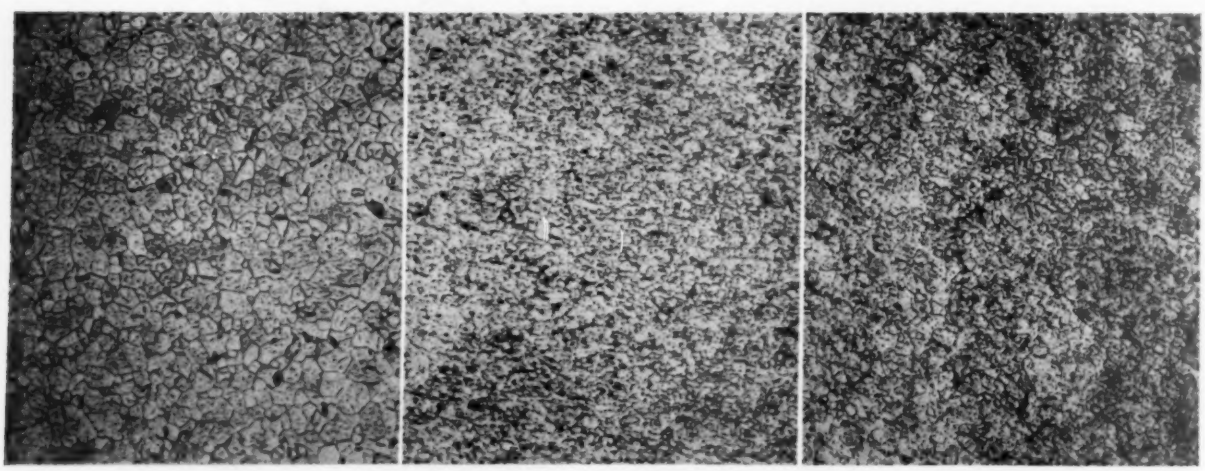


Fig. 26 Base metal structures

The as-rolled structures sectioned both longitudinal and transverse to the direction of rolling were very similar. It is interesting to note that base metal A could be readily distinguished from base metals B and C by its larger grain size. × 100 (area approximately 4 x 4 in. before reproduction).

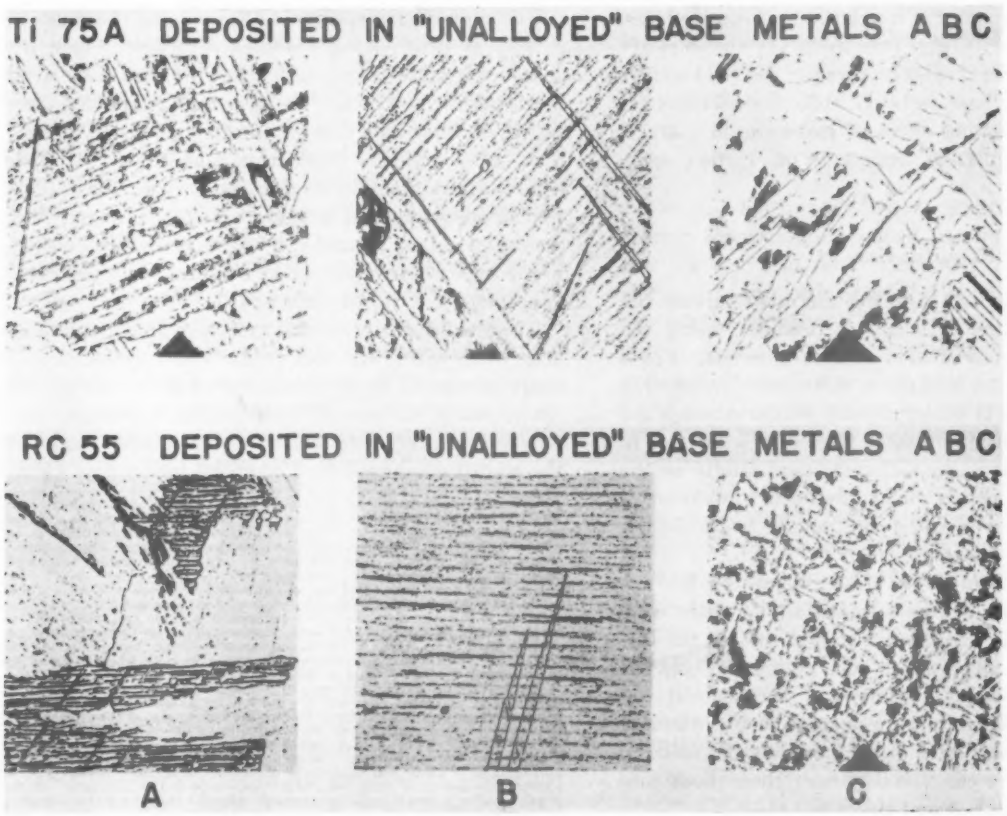


Fig. 27 Weld metal structures produced by "unalloyed" filler metal deposited in "unalloyed" base metal. × 500 (area approximately 4 x 4 in. before reproduction)

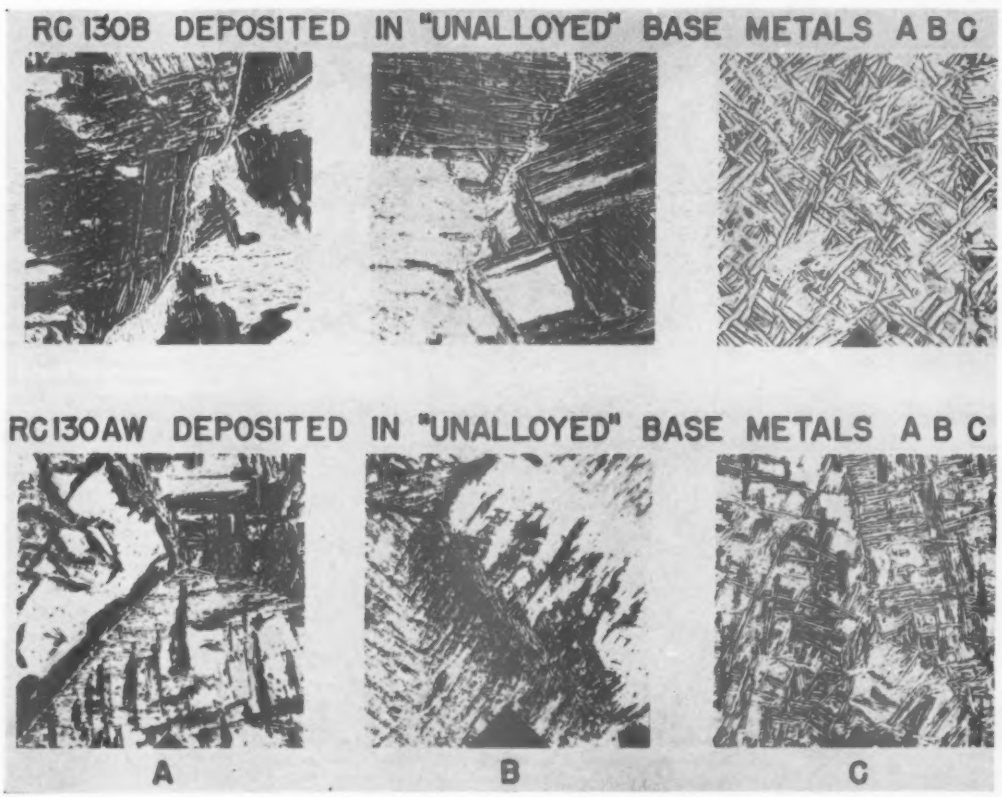


Fig. 28 Weld metal structures produced by depositing alpha-beta alloys containing manganese in "unalloyed" base metal. × 500 (area approximately 4 x 4 in. before reproduction)

The marked difference in toughness between alloy deposits in base metal C and base metals A and B is attributed to the higher carbon content of the weld metals when deposited in base metal C (0.13 to 0.16 carbon). This severe embrittling effect of carbon is in marked contrast to the relative impotence of carbon as a hardener.

Microstructural Examination

Figures 26 through 29 show the microstructures of the base metals and the weld metals produced using the various combinations of filler and base metals. For a given filler metal, the weld metal structures developed in base metals A and B were generally similar whereas the weld deposits (containing more than 0.12% carbon) in base metal C were quite different, particularly in the case of alloy filler metals. The differences in weld metal structure are believed to be due largely to the higher carbon content of base metal C.

In the case of "unalloyed" filler deposited in "unalloyed" base metal, large serrated alpha plates were prevalent (Fig. 27). In base metals A and B, the serrated plates were interspersed with grains of a lamellar precipitate frequently containing a well-defined unidentified needlelike structure suggestive of twinning. The serrated alpha plates of the "unalloyed" welds in base metal C were considerably finer than those contained in base metals A and B.

The alloy fillers deposited in "unalloyed" base metals A and B produced a complex structure which appeared to consist largely of alpha with some large areas of untransformed beta. The structure of the deposited RC-130B filler and that of the deposited RC-130AW filler were similar. Nucleation and growth of alpha from the grain boundaries was evident when these fillers were deposited in base metals A and B. However, when deposited in base metal C, the fillers produced a basketweave type of structure which appeared in a rather advanced stage of transformation.

The weld deposits in base metals A and B contained large areas within the beta grains which appeared to be untransformed. Such regions were lightly etching, soft (particularly susceptible to polishing scratches), and only very weakly birefringent under polarized light (in the etched condition).* The entire fusion zone (weld-

* Metallic surfaces can be divided into two classes: first, optically isotropic, where the optical properties are the same in all directions and, secondly, optically anisotropic, where the optical properties are different for different directions. In microscopic examination by polarized light, titanium occurs as both isotropic (beta titanium is body centered cubic) and anisotropic (alpha is close-packed hexagonal).

Under polarized light, isotropic and anisotropic regions of a surface may be distinguished from each other by observing changes in brightness during rotation of the specimen. Since beta-titanium is cubic and, therefore, isotropic, it is to be expected that beta titanium will show no birefringence at normal incidence. Alpha titanium, on the other hand, being uniaxial and, therefore, anisotropic can be distinguished from beta by observing changes in brightness during rotation of the specimen.

While there are numerous causes of the false appearance of birefringence, there is only one cause known to the authors for a birefringent metallic surface to behave as though it were optically isotropic; viz., if the surface being examined is cut approximately perpendicular to the optic axis of the hexagonal crystal. Thus, if in an alpha-titanium matrix there are numerous grains which appear isotropic (uniformly dark under crossed polarizers for all positions of stage rotation), it would be necessary to conclude that an appreciable degree of preferred orientation exists in the structure. If this is not tenable, it would seem necessary to conclude that the grains consist of untransformed beta.

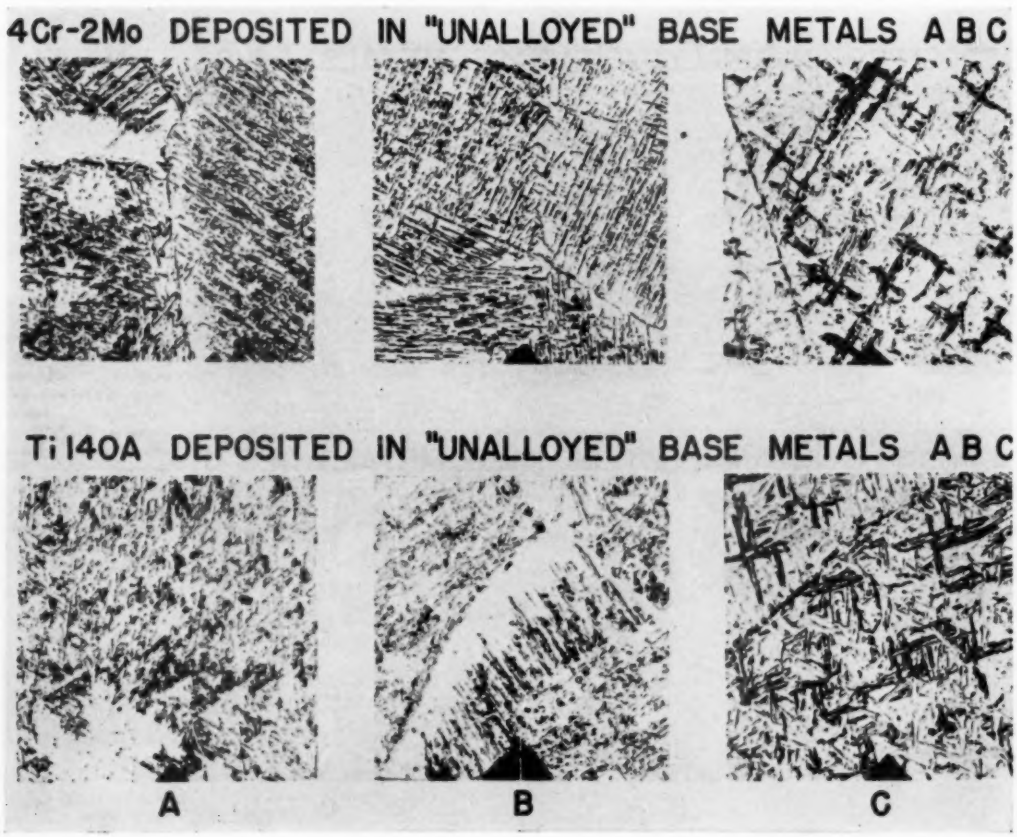


Fig. 29 Weld metal structures produced by depositing alpha-beta alloys containing chromium and molybdenum in "unalloyed" base metal. × 500 (area approximately 4 x 4 in. before reproduction)

metal base-metal interface) of the RC-130AW weld deposit in base metal B consisted of a band of such structure (Fig. 30). In the case of RC-130B deposited in base metals A and B, entire grains appeared to be untransformed. Delazaro and Rostoker[7] observed a similar phenomenon in oxygen-bearing alloys where beta grains in an advanced state of precipitation occurred adjacent to grains which apparently had not yet begun to precipitate alpha.

The transformation structures occurring in the case of the 4Cr – 2Mo and the Ti-140A fillers were quite different from those of the manganese-bearing fillers. In the first place, the alpha was very much finer and, in the case of deposits in base metals A and B, occurred in the form of serrated platelets. Secondly, nucleation apparently occurred within the grains rather than at the grain boundaries. Again there appeared to be untransformed beta, this time predominately at the grain boundaries. The structures of the Ti-140A and the 4Cr – 2Mo fillers when deposited in base metal C were entirely different from those produced by the same fillers deposited in base metals A and B; the basket-weave structure was again in evidence but the transformation appeared to be in a less advanced stage.

In only two cases was there an appreciable difference in structure between the weld passes corresponding to Row 1 and Row 2 hardness traverses. The lower hardness of the Row 2 traverse in the case of RC-130AW filler deposited in base metal A was associated with a finer alpha structure; and the lower hardness of the Row 1 traverse in the case of the RC-130B filler in base metal C was associated with random clusters of a fine nodular precipitate.

SUMMARY—PART II

Notched-bar impact tests conducted over a range of testing temperatures proved by far the most sensitive test to weld embrittlement. In impact tests of the unwelded base metals and the "unalloyed" welds, the relative positions of the transition curves were consistent with the total gas content (oxygen, hydrogen and nitrogen) of the base metals but not with the COHN. Thus, the carbon in base metal C and the carbon in the "unalloyed" welds in base metal C was less effective than the gases in promoting notch sensitivity in all-alpha base materials and welds. Nevertheless, all base metals and "unalloyed" weld deposits were considered to be seriously deficient in notch toughness. The consistently poor performance of the "unalloyed" welds was noteworthy in light of the low interstitial content of the "unalloyed" fillers used. The marked influence of base metal COHN on the toughness of the weld deposits was apparent. The toughness of the alpha-beta weld deposits in the two low-carbon base metals (less than 0.10%) was greatly increased over that of the "unalloyed" all-alpha weld deposits. With one exception, the 10 ft-lb transition temperatures of the

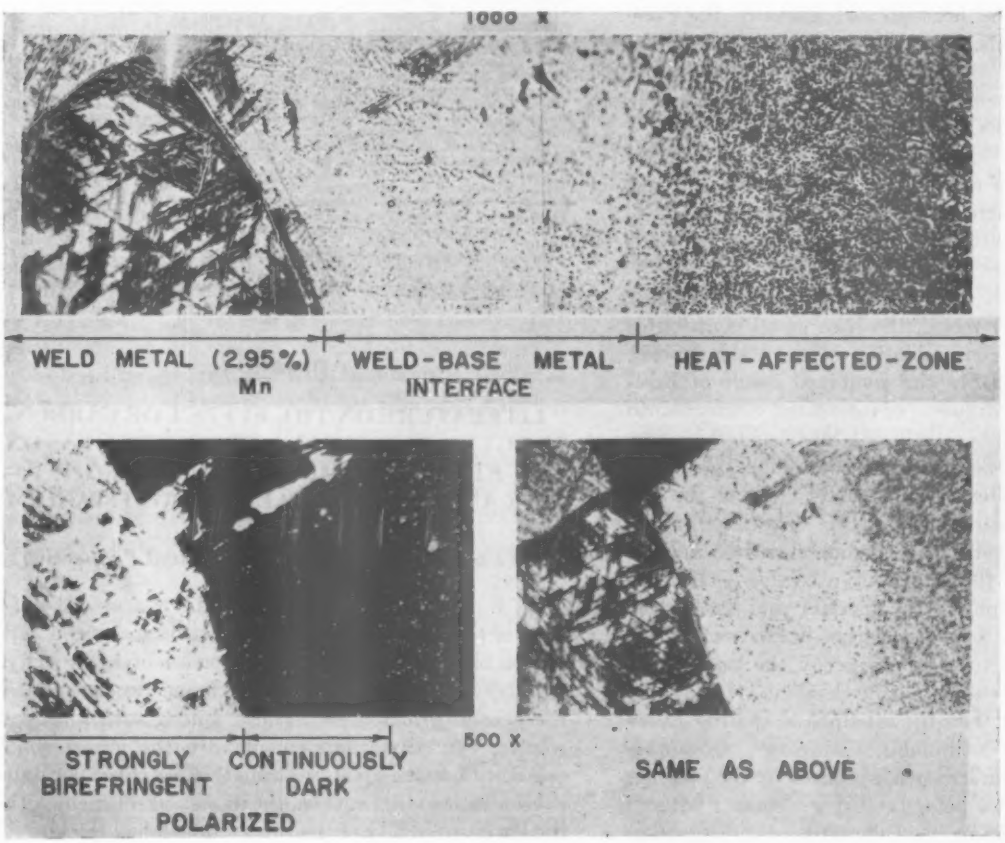


Fig. 30 Fusion zone structure

Lightly etching areas largely devoid of precipitate were observed in all alloy weld deposits in base metals A and B. Because the areas were soft and very weakly birefringent under polarized light, it appeared that the areas were untransformed beta. × 1000 (area approximately 10 x 3 in. before reproduction).

alpha-beta weld deposits occurred at −80° C or below; this was in marked contrast to the "unalloyed" welds with transitions above room temperature. However, the alpha-beta weld deposits in the base metal containing 0.24% carbon were even more embrittled than the all-alpha welds. This severe embrittling effect of carbon on alpha-beta alloys is in marked contrast to the relative impotence of carbon as a hardener. The embrittling carbon-content was between 0.09 and 0.14%.

Microstructural examination of the weld deposits showed the welds in base metals A and B to be decidedly different in structure from those in base metal C. The difference was attributed to the higher carbon content of the welds in base metal C (average 0.14%). The manganese-bearing welds produced with RC-130AW and RC-130B filler were similar in that nucleation and growth of alpha from the grain boundaries was indicated and the welds produced with the 4Cr – 2Mo and Ti – 140A fillers (2Cr – 2Mo – 2Fe) were similar in that the alpha was finer and appeared to precipitate and grow from within the grains. In both types of weld structures and in the "unalloyed" weld heat-affected zones, large areas were observed which were soft and weakly birefringent or continuously dark on rotation under polarized light. These areas are tentatively identified as retained beta (isotropic body-centered-cubic titanium). The retained-beta areas occurred within the grains of the manganese bearing welds, at the grain boundaries of the Cr-Mo bearing welds and as entire grains in the heat-affected zones of the "unalloyed" base metals.

CONCLUSIONS

Determination of the carbon, oxygen, hydrogen and nitrogen content, hereinafter referred to as the COHN, is essential to all future research on titanium, particularly when metal-arc welding is involved.

When the COHN of the base metal is high relative to that of the filler metal, the base metal contributes materially to the contamination of the weld deposit. This effect is probably the combined result of base-metal melting and diffusion of interstitial elements into the weld from the base metal due to concentration gradient. Hardness, bend, tensile and impact tests all show the marked effect of base metal COHN on the performance of deposited weld metal. The notched-bar impact test, in particular, demonstrates a marked embrittlement as the result of excessive COHN in "unalloyed" all-alpha weld deposits and in alloyed alpha-beta welds as a result of carbon in excess of 0.10%.

Alpha-beta structures produced by the presence of small amounts of beta-stabilizing elements are markedly more notch tough then the all-alpha structures of so-called "unalloyed" titanium. Moreover, an appreciable gain in tensile strength is realized by the presence of small amounts of beta-stabilizing elements without detriment to tensile or bend ductility. And most important of all, it appears that a higher COHN (with the possible exception of carbon) can be tolerated by alpha-beta structures.

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Appendix

LITERATURE ON THE EFFECT OF CARBON, OXYGEN, HYDROGEN AND NITROGEN ON THE STRUCTURE AND PROPERTIES OF IODIDE AND COMMERCIALLY PURE TITANIUM

Effect of Interstitials on Mechanical Properties

W. L. Finlay and J. A. Snyder[8] investigated the effects of three interstitial solutes—nitrogen, oxygen and carbon on the mechanical properties of high-purity (iodide) alpha titanium. It was found (in contrast to the general principle established among substitutional solutes, viz., that the element with the lowest solid solubility has the greatest strengthening effect per unit concentration) that carbon, the interstitial element with the lowest solubility of the three studied, had the lowest

strengthening effect.* In connection with the extraordinarily potent effects of nitrogen and oxygen on the mechanical properties of titanium, the authors noted that concentrations of less than 0.1 wt. % of the elements more than doubled the strength and halved the ductility of high-purity titanium. They concluded from this that researchers must be ceaselessly vigilant against the accidental intrusion of these elements.

R. I. Jaffee, H. R. Ogden and D. J. Maykuth[9] also investigated alloys of titanium with carbon, oxygen and nitrogen. The study was made on the same high-purity iodide titanium used by Finlay and Snyder. The following mechanical properties were found for the iodide titanium:

Average Mechanical Properties of Nine Annealed Iodide Titanium Specimens

Proportional limit, psi	11,000
0.1% offset yield, psi	23,200
0.2% offset yield, psi	27,000
Ultimate strength, psi	43,200
Elongation (1-in.), %	40
Reduction in area, %	61
Vickers hardness (10 kg)	105

From data of the above referenced work the effects of 0.1% carbon, oxygen and nitrogen on the mechanical properties of iodide titanium were estimated to be as follows:

Effect of the Interstitial Elements per Unit Concentration on Mechanical Properties of Iodide Titanium

	0.1% C	*0.1% O*	*0.1% N*
0.2% offset yield, psi	38,000	45,000	69,000
Ultimate strength, psi	54,000	63,000	80,000
Elongation (1 in.), %	38	30	20
Reduction in area, %	49	49	46
Vickers hardness (10 kg)	140	160	190

Thus, the authors found nitrogen to have the greatest strengthening and hardening effect, oxygen next and carbon least. The reverse generally held true for ductility.

As regards embrittlement in the tensile test, the authors observed that up to about 1% alloy content, carbon-containing alloys showed no sign of becoming brittle; at about 0.75% oxygen the Ti-O alloys were embrittled; and in the Ti-N alloys brittleness set in somewhere between 0.2 and 0.5%, with the actual value estimated to be closer to 0.5% nitrogen.

Effect of Interstitial Content on Structure

Quenching iodide titanium from the beta field results in a structure characterized by large serrated Widmenstätten plates formed during cooling through the transformation range. Jaffee, *et al.*,[9] have shown (1) that iodide titanium alloyed with carbon quenched from the beta-carbide field (920° C and above) developes rounded particles of titanium carbide distributed in a basket-weave matrix; (2) that iodide titanium alloyed with oxygen also develops a basket-weave structure rather than the serrated-plate Widmanstätten structure and (3) the iodide titanium alloyed with nitrogen transforms to an acicular basket-weave type of structure. Since the solid solubility of hydrogen in alpha decreases from about 0.16 wt. % to about 0.004 wt. % from 300 C to room temperature at concentrations sometimes present as an impurity in titanium (0.06 wt. % and greater), titanium hydride may be expected to appear in the microstructure as an insoluble TiH phase, giving rise to the characteristic "line-markings" which are observed in the structure of alpha titanium.[10] The hydride phase cannot be retained in solution on quenching, but precipitates as a fine dispersion. Upon subsequent aging at temperatures as low as 100° C a noticeable agglomeration of the hydride has been observed, the particles being cubic or rectangular in shape. Upon further heating and aging fine Widmanstätten platelets develop. Alpha-beta alloys, on the other hand, do not show the line markings. Since the solubility of hydrogen is greater in the beta phase than in alpha, hydrogen in an alpha-beta alloy is thought to be dissolved in the beta phase.

The Effect of Hydrogen

The hydrogen content of most commercial titanium appears to be about 0.0050 wt. % (50 ppm). Battelle investigators[11] found that vacuum annealing provided a positive solution to hydrogen embrittlement when the hydrogen content was in excess of 50 ppm. The relatively small difference between the impact strength of vacuum annealed and commercial-purity titanium containing 0.0055 wt. % hydrogen as compared to the marked improvement in toughness afforded by a vacuum anneal of iodide-purity titanium which had been alloyed with about the same amount of hydrogen, was taken to indicate that either the hydride phase did not precipitate in the commercial-purity titanium or that there was a redistribution of the hydride. The absence of hydride from the microstructure of even slow-cooled commercial titanium with 0.0055 wt. % hydrogen was taken to indicate that the hydrogen was in solution in the globular retained-beta phase which is usually present with a residual of 0.10% iron or greater. Since the retained-beta phase results from the iron impurity in commercial titanium, it would be expected that the room temperature solubility of hydrogen would vary according to the amount of iron impurity. The Battelle investigators pointed out that the relatively high solubility of hydrogen in retained beta suggests the use of retained beta as a hydrogen getter. However, they did not recommend such a practice since they expected the hydrogen-saturated beta phase to be embrittled. They qualified this limitation, however, by pointing

* It was noted by Finlay and Snyder that of the three interstitials studied, carbon had a marked tendency to change the c_0/a_0 ratio toward the theoretical 1.63. Assuming the latter to be associated with easier slip, the authors suggested that the closer approach to the c_0/a_0 ratio of hexagonally close-packed spheres may have compensated for the lattice expansion expected with the less favorable atomic diameter of carbon.

out that if the beta were present as intragranular globules, the embrittlement might not be as great as when the beta was intergranular.

The impact resistance of high-purity (iodide) titanium was investigated at Battelle Memorial Institute.[12] To minimize the hydrogen content, the material was vacuum annealed for 5 hr at 900° C. The resulting composition follows: Carbon, 0.02; oxygen, 0.025; and hydrogen, 0.0010. A comparison of the impact resistance with and without vacuum annealing demonstrated the marked effect of hydrogen on impact properties.

Comparison of Iodide-Titanium Impact-Energy Values with and Without Benefit of Vacuum Annealing

Hydrogen content, ppm	*Impact-energy values,* in.-lb, at temperatures of*			
	−196 C	*−80 C*	*−40 C*	*25 C*
90	27	..	26	22
10	119	125	129	132

* Serrated alpha structures (approximately 0.25-mm grain size) obtained by quenching from the beta field.

Effect of Interstitial Elements on Reaction Rates

Investigators at Armour Research Foundation[13] have observed that the presence of interstitial impurities of carbon, oxygen and nitrogen exert a profound influence on solid state reaction rates. For example, a comparison was made between the isothermal decomposition of a 5% Mo binary and a 5% Mo – 0.5% C ternary alloy. Examination of the latter alloy showed fine nodular carbide uniformly dispersed in the matrix. The presence of the carbides signified that the beta phase was saturated with carbon. A comparison between the TTT diagrams for the Ti-Mo and the Ti-Mo-C alloys showed a strong accelerating influence on the reaction rates imposed by the carbon. However, regardless of the heat treatment, the carbide inclusions appeared to take no part in providing nucleation centers for the precipitation of alpha. The carbide inclusions were therefore considered inert. However, the precipitates of alpha were very much finer in the ternary alloy than in the binary alloy. It was concluded, therefore, that the presence of the carbide acted as a grain-growth inhibitor (Fig. 31). The presence of 0.5% C, however, lowered the M_s temperature and greatly accelerated the times for completion of transformation in the Ti-Mo alloy (Fig. 32).

For further information on the effect of interstitial elements on reaction rates in titanium, controlled amounts of oxygen (0.15, 0.35 and 0.55 wt. %) were added to a Ti – 11% Mo alloy. For purposes of comparison a binary Ti-Mo alloy was prepared using iodide titanium. The following observations were made (see Fig. 32).

(*a*) The times for visible initiation of transformation were shortened by oxygen additions. The effect was most predominant in the 11% Mo alloy above the "knee" of the *C*-curve. At the higher oxygen levels, transformation began so rapidly that it was difficult to suppress except by the most vigorous quenching.

(*b*) The times for completion of transformation were accelerated at temperatures above the "knee" of the *C*-curve. In some instances an acceleration factor of 20 was produced. It was also noted that the rejected alpha in oxygen-rich alloys was invariably finer than in the purer alloys.

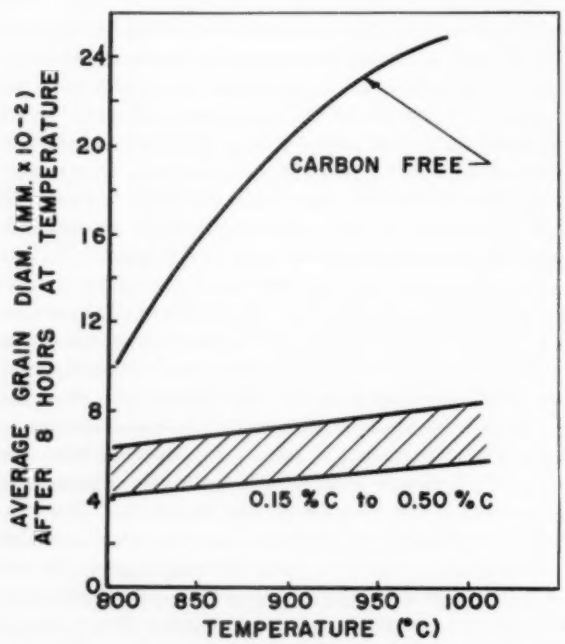

Fig. 31 Grain-growth characteristics of Ti – 11% Mo alloy. After Levinson, McPherson and Hansen

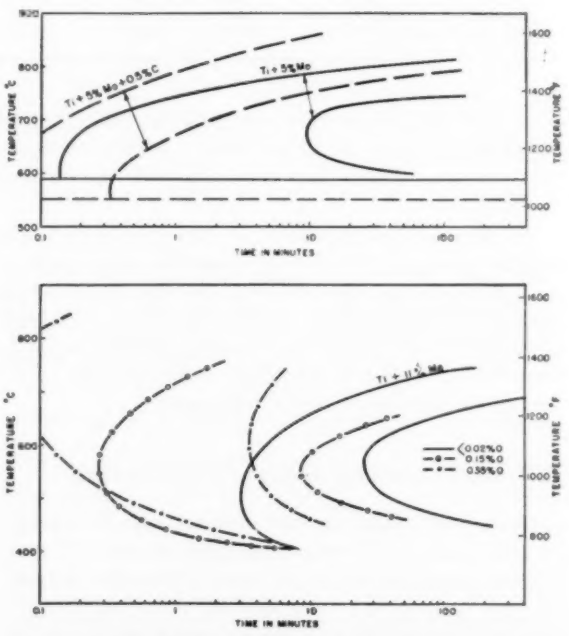

Fig. 32 Effect of interstitial elements on time-temperature-transformation in Ti-Mo alloys. After Rostoker, McPherson and Hansen

Table 18—Effect of Carbon and Carbon-Oxygen on Mechanical Properties

	Iodide	Alloying element: 0.16% C	0.27% C	0.47% C	0.19% C–0.12% O	0.38% C–0.20% O
		950° C. quench				
Ultimate, psi	38,000	52,700	50,600	63,900	65,400	78,100
0.2% yield, psi	25,000	41,200	39,500	49,000	50,000	60,800
P. L., psi	14,000	23,400	23,400	38,600	30,600	39,500
Hardness, Vhn	95	151	138	164	188	187
Impact, in.-lb	146	41	29	48	27	12*
R. A., %	88	65	61	66	46	40
Elongation, %	70	36	42	42	29	30
		850° C. quench				
Ultimate, psi	35,000	55,200	62,100	80,000	78,000	98,300
0.2% yield, psi	18,000	49,900	60,300	75,200	76,200	98,000
P. L., psi	7,500	48,900	56,700	71,800	72,500	88,000
Hardness, Vhn	88	164	190	225	219	275
Impact, in.-lb	155	52	53	33	33	6
R. A., %	90	56	58	61	63	46
Elongation, %	80	49	46	51	41	38

* Quenching from 1000° C gave an impact-energy value of 36 in.-lb.

(*c*) The presence of oxygen appeared to have relatively little effect on the reaction rate at temperatures below the "knee" of the *C*-curve Regarding the origin of oxygen hardening, it was concluded that oxygen contributes to both solid solution hardening and to structural hardening.

Effect of Carbon on Mechanical Properties

An investigation was conducted at Battelle Memorial Institute[14] on the microstructure and heat treatment of Ti-C alloys. It was found that in the titanium-carbon system (peritectoid type Fig. 33) the chief microstructural variable which influences the properties is solid-solution strengthening (Table 18). When in solid solution, the carbon makes a definite contribution to the strength of the alloy (Fig. 34). However, when the carbon is present as Ti-C, it has very little effect on the strength. Other microstructural variables such as transformation structures and grain size appeared to have only a minor effect on properties as compared to the solid-solution effect.

The quantity of carbon in solid solution was controlled by heat treatment. Quenching from temperatures just below the peritectoid temperature retains the maximum amount of carbon in solid solution. Quenching from the beta-carbide field, on the other hand, places the alloy in a softer condition than it is in the alpha-annealed condition because the solubility of carbon in beta titanium is quite low. Thus, a quench from the beta field produces a soft alpha matrix. Tensile ductility was not changed appreciably by annealing within the alpha field but quenching from the beta-carbide field lowered the reduction in area and elongation slightly. Notch toughness, on the other hand, was reduced by carbon in solid solution (Fig. 35); i.e., as the

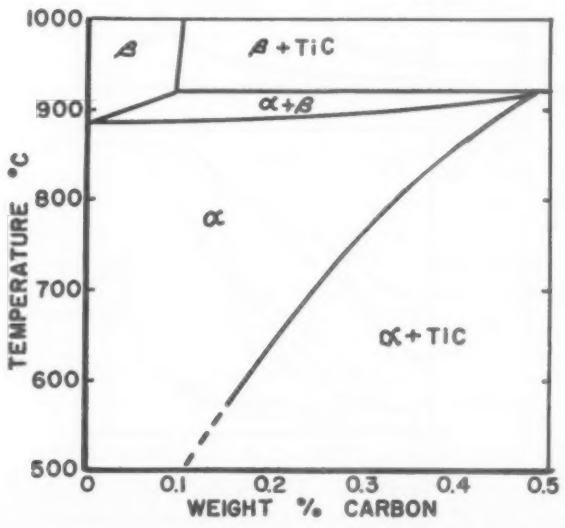

Fig. 33 Titanium-rich portion of titanium-carbon phase diagram. After Ogden, Holden and Jaffee

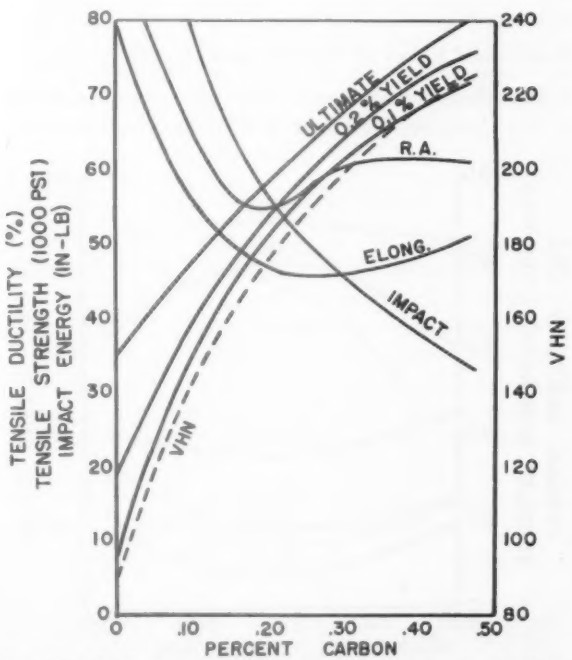

Fig. 34 Mechanical properties of high-purity Ti-C alloys quenched from 850° C. Data of Ogden, Holden and Jaffee

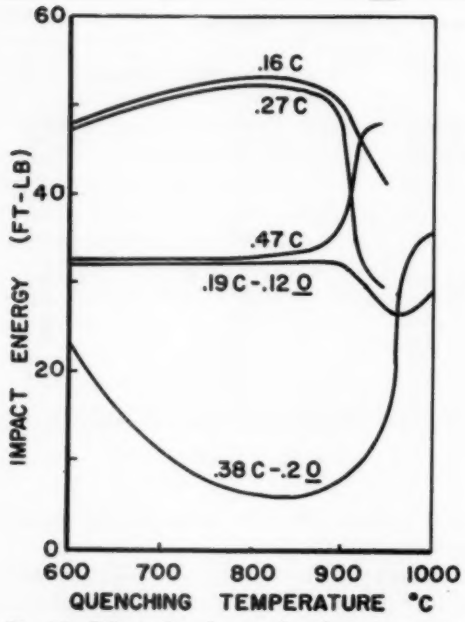

Fig. 35 Effect of carbon and carbon + oxygen on toughness of high-purity titanium. Data of Ogden, Holden and Jaffee

quantity of carbon in solid solution was increased, the tendency for a transition from high-to-low energy increased. However, when the alloys were annealed in the beta-carbide field, which resulted in a very small amount of carbon in solid solution, there was little change in impact value with testing temperature. The effect of increasing the grain size from 0.02 to 0.10 mm in the Ti – 0.16C alloy was investigated. The energy-impact values increased slightly with increasing grain size.

Combined Effect of Carbon and Oxygen

Two alloys contaminated with oxygen during melting in the Battelle investigation[14] were investigated to determine the combined effect of the interstitial elements carbon and oxygen (0.19 % C – 0.12% *O* and 0.38% C – 0.2% *O*). Since oxygen is an alpha-stabilizing element with high solubility in both alpha and beta titanium, the Battelle investigators hypothesized that the combination of oxygen and carbon should be expected to be additive in strengthening effect. The increase in strength and loss of impact-energy (over that of iodide-purity titanium see Fig. 36) produced by various carbon and carbon-plus-oxygen additions is demonstrated in Table 18 and Fig. 35.

A study was made of the effect of testing temperature on the impact properties of the iodide-base Ti-C-O alloys when quenched from various temperatures. At room temperature and below, the impact-energy values decreased as the quantity of carbon in solid solution was increased. In the case of the 0.38% C – 0.2% *O* alloy in the 850° C quenched condition (maximum carbon in

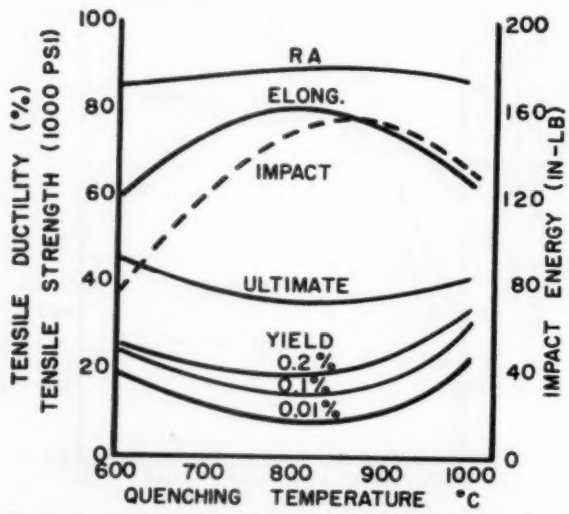

Fig. 36 Properties of high-purity (iodide) titanium. Data of Ogden, Holden and Jaffee

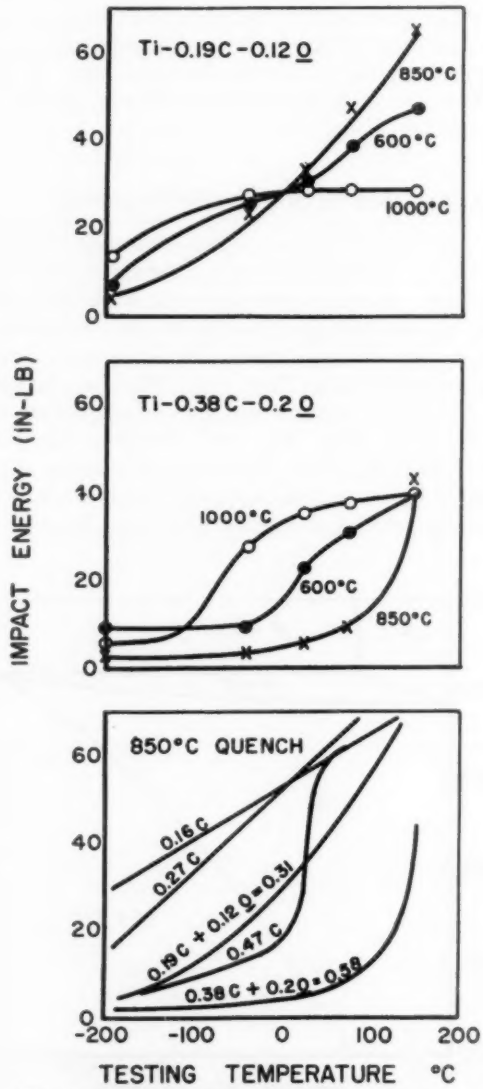

Fig. 37 Effect of testing temperature on notch toughness of titanium containing various levels of interstitial elements in solid solution. After Ogden, Holden and Jaffee

solid solution), the transition from high-to-low energy values occurred at about 125° C; whereas, in the 1000° C quenched condition, where there is the least carbon in solution, the transition appeared to be below −50° C.

The authors observed that the tendency toward a ductile-to-brittle transition in the impact-energy curve appeared to be dependent more on the total interstitial content in solid solution in the alloy than on the particular interstitial element present. From Fig. 37 summarizing the effect of testing temperature on the impact properties of iodide-base titanium containing various levels of interstitial elements in solid solution (quenched from 850° C), it was observed that as the interstitial content increased, there was an increased tendency toward a transition behavior until finally a decided transition occurred.

Effect of Interstitials on an All-Alpha-Alloy

The effects of small additions of carbon, oxygen and nitrogen on the mechanical properties of a single-phase all-alpha alloy containing 5% Al – 2.5% Sn was investigated by Rem-Cru Titanium, Inc.[15] Using a low-hardness sponge base (40,000 psi yield strength), the tensile strength was 111,000 psi as compared with 75,000 psi in an iodide-base 5% Al alloy (48% increase in strength due to interstitial content of the sponge and the addition of tin). Chemical analysis indicated 0.05% carbon and 0.021% nitrogen in the sponge-base alloys without deliberate interstitial additions (unfortunately, vacuum-fusion analyses were not made). The objective of the study was to determine the effects of small controlled additions (made to sponge-base alloy) of three interstitial elements (carbon, oxygen and nitrogen) on the mechanical properties. In particular it was desired to know the quantitative effect of controlled additions of these elements in order to ascertain the maximum amount which might be tolerated without appreciably reducing the toughness of the alloy.

Nitrogen was found to be the greatest strengthener, followed by oxygen and then carbon. The strengthening effect (increase in ultimate tensile strength over that of sponge-base alloys without deliberate interstitial addition) per 0.1% of interstitial element follows: Nitrogen, 40,000 psi; oxygen, 30,000 psi; and carbon, 10,000 psi. Tensile ductility, although somewhat lower than that of the base composition, was still appreciable in the presence of contaminants up to approximately 0.10% N, 0.10% *O* and 0.30% C (specimens averaged 15% elongation and 33% reduction in area).

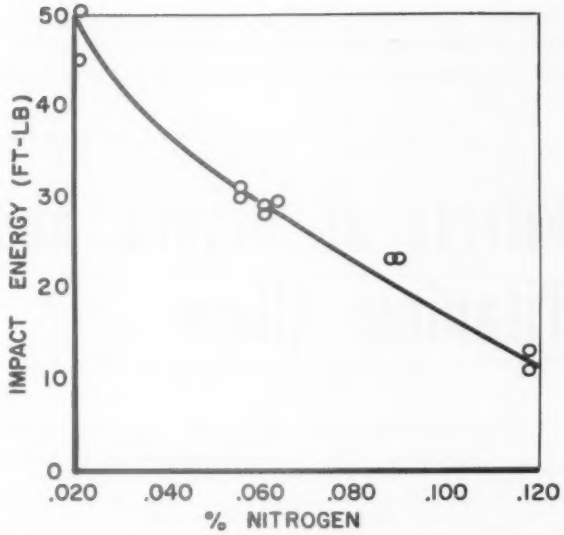

Fig. 38 Effect of nitrogen on toughness of all-alpha Ti – 5Al – 2.5Sn alloy. After Rem-Cru Titanium, Inc.

The increase in tensile strength provided by the addition of interstitial elements was attended by a marked loss of toughness, Table 19. For example, when the nitrogen was increased to 0.089%, the V-notch Charpy impact energy dropped from 48 ft-lb for uncontaminated sponge-base alloy to 23 ft-lb (Fig. 38). When the oxygen was increased to 0.10% (nominal composition), the impact energy dropped to 20 ft-lb and when the carbon was increased to 0.26%, the impact energy dropped to 14 ft-lb. When both carbon and oxygen were added (0.18% C and 0.10% *O*), the impact energy dropped to 12 ft-lb.

Thus, it was demonstrated that although the deliberate addition of interstitial elements to the all-alpha 5% Al – 2.5% Sn alloy produced the desired strengthening, the notch toughness of the alloy was seriously impaired by interstitial elements within the range of contents often encountered in commercial practice.

All-Alpha Versus Alpha-Beta Alloys

A study of sponge-base (140 Bhn maximum) Ti-Al alloys at Battelle[16] showed a tensile strength of 102,000 psi for the 5% Al alloy (highest practical aluminum content) with a room temperature notch toughness of approximately 20 ft-lb in the base metal and 30 ft-lb in the as-welded condition. A comparison was made between all-alpha and alpha-beta alloys containing manganese and aluminum. The tensile strength was

Table 19—Effect of Interstitial Elements on Tensile and Impact Properties of All-Alpha Alloys

Alloy	*Yield 0.2% offset, psi*	*Ultimate strength, psi*	*Elongation, %*	*Reduction in area, %*	*Impact energy, in.-lb* R. T.	−40° C
Iodide-base, 5% Al	60,000	75,000	18	42	..	..
Sponge-base, 5% Al-2.5% Sn	103,400	111,000	19	46	48	35
Sponge-base, 5% Al-2.5% Sn, 0.089% N	124,200	133,550	14.5	21.5	23	15
Sponge-base, 5% Al-2.5% Sn, 0.18% C. 0.10% *O*, 0.028% N	138,000	144,000	14	28	12	10

increased from 90,000 psi for an alloy containing 4% Al to 135,000 psi for an alloy containing 3% Mn – 4% Al. The toughness of the alpha-beta alloy was as good as the all-alpha alloy with approximately 20 ft-lb in the base metal and 29 ft-lb in the as-deposited weld metal (room-temperature test).

Effects of Oxygen and Nitrogen in Welding Titanium Alloys

Maximum tolerable oxygen and nitrogen contamination levels in welds in various alloys of titanium as determined through simulated welds in these alloys

by James H. Johnston

IN CONTRAST with unalloyed titanium which can be welded to produce ductile joints, most commercial titanium alloys are embrittled upon welding.

Oxygen and nitrogen contamination of the weld is partly responsible for this embrittlement, but even when welding has been conducted to avoid oxygen or nitrogen contamination, the thermal heating and cooling cycles, incurred by welding, cause embrittlement.

No heat treatments have yet been found that can restore ductility to weldments in titanium or its alloys once they have been contaminated by oxygen or nitrogen. On the other hand, when there has been no atmospheric contamination during welding, certain titanium alloys can be rendered ductile by heat treatment.[1, 2]

It is necessary that heat treatment studies of titanium alloy weld structures be preceded by proper welding techniques to prevent any oxygen or nitrogen contamination.

The amount of oxygen that can be tolerated in the base material of unalloyed titanium, yet still permit ductile welds, has been demonstrated to be about 0.3% oxygen, although as little as 0.15% oxygen causes noticeably poorer physical properties. An estimated 0.05% nitrogen content appears to be the maximum tolerable in unalloyed titanium weldments.[3]

It is the purpose of this paper to present the maximum oxygen and nitrogen contamination levels in welds in various alloys of titanium.

James H. Johnston is Melting Research Engineer, Mallory-Sharon Titanium Corp., Niles, Ohio.

EXPERIMENTAL TECHNIQUES

The samples tested are weld beads alone, laid down in a groove in a water-cooled copper block in order that the oxygen or nitrogen pickup might be estimated from the weld bead weight and amount of contaminating gas in the welding chamber. Such "weld beads" approximate what would be obtained on welding heavy sections of titanium, and represent the only section of a weld which is altered by contamination.

The welding chamber had a very small volume with all the valves and gas access holes close to the chamber in order to provide unobstructed access of the contaminating gases to the weld. Figure 1 is a photograph of this equipment. All joints in this furnace were surrounded by an evacuated space, and all metal parts were solder-tinned to prevent leakage. The gas system was of all-glass construction with an activated charcoal liquid nitrogen trap for removing traces of contaminants from the helium.

A previously weighed sample of the desired alloy, chopped up into 1/4-in. pieces, was placed in the slot pounded into the copper hearth and welded with a tungsten tipped electrode. It was found that 3 passes, each followed by cooling, at 150 amp were sufficient to pick up all the intentionally added contaminating gases at which time no color remained. A light tan temper color remaining on the cooled weld bead indicated less than 0.01% oxygen remaining in the chamber.

All samples were tested in tension at room temperature at a cross-head speed of 0.04 ipm. Available analysis and physical properties of the as-received materials are shown in Table 1.

Table 1

Alloy trade symbol		*Heat Number*	*Chemical analyses (by weight), %*						*Tensile test properties (in as-received condition)*		
			C	*N*	*Fe*	*Cr*	*Mn*	*Al*	*Yield stress, psi*	*Ultimate tensile strength, psi*	*Elongation, % in 2 in.*
Ti	125-A	X1029	0.05	0.08	0.9	1.8	...	...	...	138,800	31
Ti	150-A	L630	0.05	0.08	1.5	218	...	...	...	156,000	20.0
RC	130-A	A3260T	0.1	0.03	...	...	7.5	...	117,800	129,200	19.0
RC	130-B	133130	0.13	...	...	...	3.9	2.4	135,300	143,700	18.1

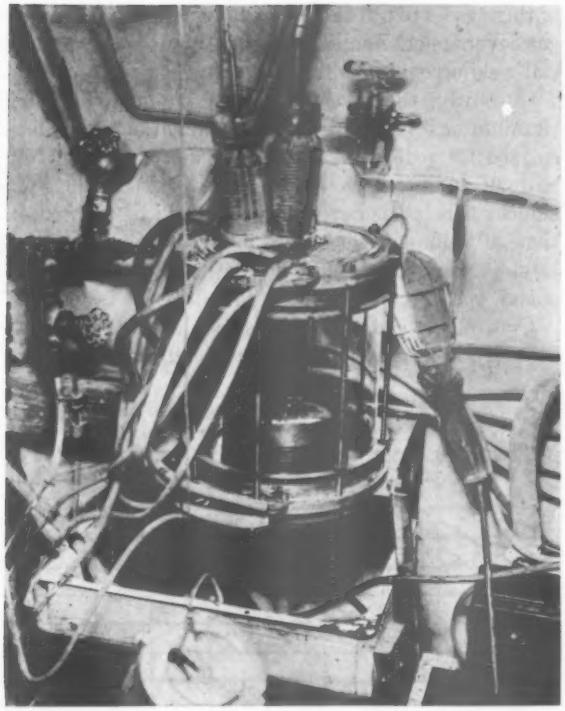

Fig. 1 Arc welding chamber for studies of simulated welds in titanium alloys

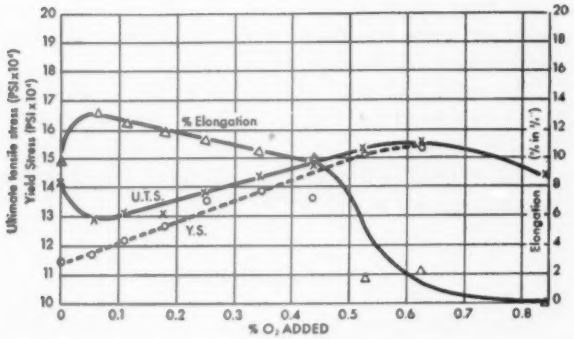

Fig. 2 Effect of oxygen on tensile properties of simulated welds in 130-A (8% Mn)

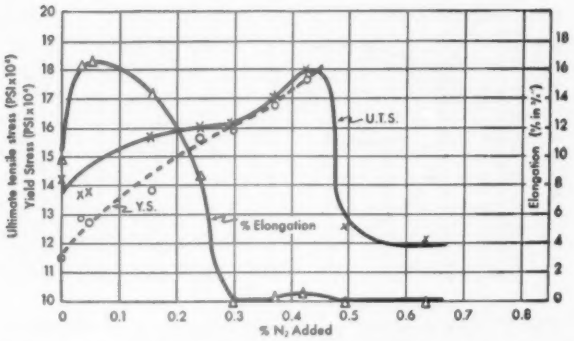

Fig. 3 Effect of nitrogen on tensile properties of simulated welds in 130-A (8% Mn)

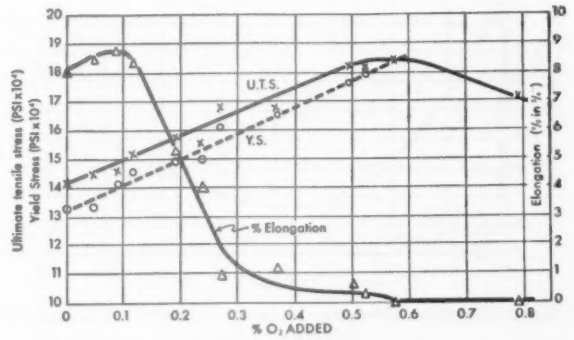

Fig. 4 Effect of oxygen on tensile properties of simulated welds in 130-B (4 Mn-4 Al)

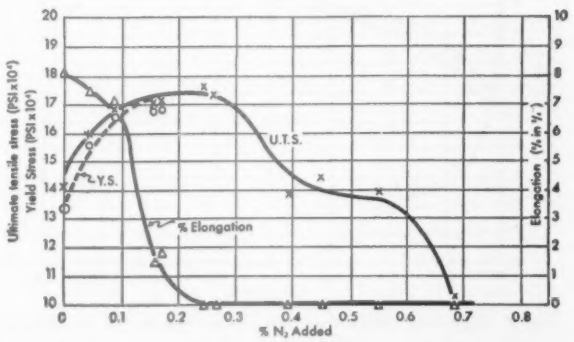

Fig. 5 Effect of nitrogen on tensile properties of simulated welds in 130-B (4% Mn-4% Al)

RESULTS

Examination of Figs. 2 through 10 reveals that alloy 130-A permits the greatest oxygen or nitrogen pickup in the weld metal. Alloy 130-A can stand up to 0.4% added oxygen and almost 0.2% added nitrogen in the weldment. (See Figs. 2 and 3.)

Although alloy 130-B cannot tolerate as much oxygen or nitrogen as alloy 130-A, the tolerable limits of gas pickup shown in Figs. 5 and 6 are within the scope of machine welding equipment. In this case 0.15% added oxygen and 0.1% added nitrogen seem to be the limits.

Comparison of the effects of air (see Fig. 6) versus the effects of oxygen (see Fig. 4) and nitrogen (see Fig. 5) separately, on 130-B suggest that air behaves in a similar manner to oxygen or nitrogen.

Though the weldments were ductile with large amounts of oxygen and nitrogen, it is not inferred that these quantities would be tolerable in fabricated sheet or rod. Interstitials dramatically increase the difficulty of working titanium alloys, and markedly reduce the bend and formability characteristics.

The other alloys tested, 125-A and 150-A, are adversely affected by almost negligible oxygen or nitrogen pickup.

The foregoing results are intended only as rough approximations since actual vacuum fusion analyses were not performed. Since the "as-received" oxygen analyses are unknown, the figures are not total oxygen content, but additions only.

The samples are not truly welds in the sense of joining two plates together. Thus cooling rates only approxi-

mate what would be obtained on welding heavy sections, and grain orientation due to the manner of solidification may also be different.

The weld was not constrained during cooling and hence weld bead cracking was eliminated as a variable. Finally, testing was done in tension only and nothing can be inferred as to the effects of oxygen or nitrogen on impact behavior of welds.

CONCLUSIONS

Alloys 130-B and 130-A, while very susceptible to oxygen and nitrogen contamination, can stand enough of these gases to put these two alloys within the range of gas coverage obtainable with slightly modified commercial welding equipment.

Currently, the first test of any completed weld in titanium or its alloys is visual examination of the bead surface for oxidation. A bright shiny metallic bead without any "temper" colors is considered evidence for sufficient inert gas coverage. It should be pointed out that all weld beads examined in this investigation were bright, shiny and metallic without any "temper" colors, yet oxygen pickup could still be excessive.

Control of the degree of oxidation is necessary as the first step in any titanium welding program.

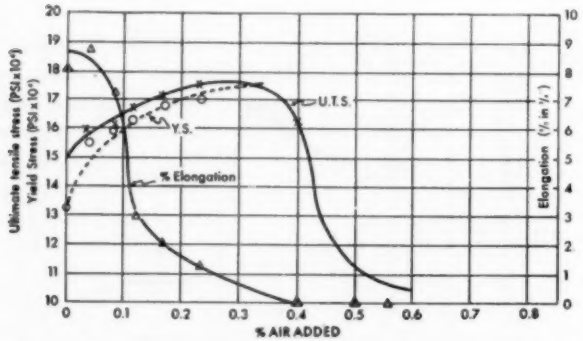

Fig. 6 Effect of air on tensile properties of simulated welds in 130-B (4% Mn-4% Al)

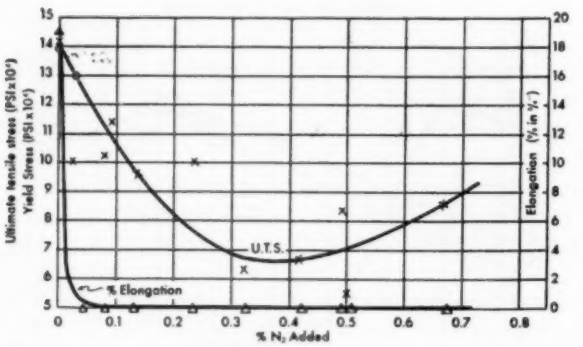

Fig. 7 Effect of nitrogen on tensile properties of simulated welds in Ti 125-A

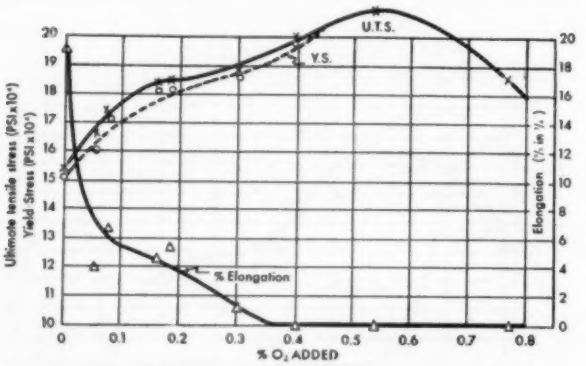

Fig. 8 Effect of oxygen on tensile properties of simulated welds in Ti 125-A

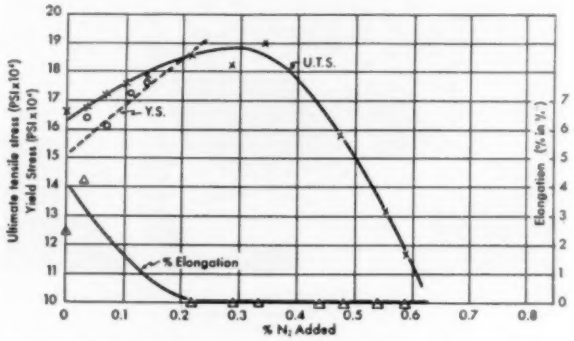

Fig. 9 Effect of nitrogen on tensile properties of simulated welds in Ti 150-A

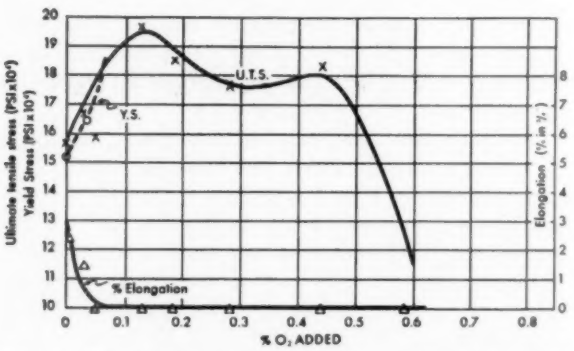

Fig. 10 Effect of oxygen on tensile properties of simulated welds in Ti 150-A

ACKNOWLEDGMENTS

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Weldments in the Titanium-Manganese Sheet Alloy RC-130A

Influence of heat treatment in overcoming embrittlement in weldments in commercial titanium sheet alloy containing 6.5 to 8% manganese; effects of surface contamination

by H. M. Meyer

Abstract

The commercial titanium sheet alloy RC-130A contains approximately 6.5 to 8% manganese and is known to embrittle after welding. The phase relationships of the titanium-rich end of the binary titanium manganese system are discussed with a view to the structural changes which occur in the various zones of an inert-arc, tungsten-electrode arc weld in sheet of 1/16 in. thickness. The occurring transformation structures, such as retained and brittle β, coarse and irresolvable α, are consistent with those observed in the other eutectoid type titanium systems. The significance of particular microstructures in the weldment are discussed as indicative of physical properties. It is also shown that this important relationship enables us to restore to the weldment the ductility of the unwelded sheet by appropriate heat treatments. Two types, the solution treatment followed by an isothermal anneal, and the simple $\alpha + \beta$ treatment, are shown to be suitable. Finally, the effects of surface contamination and of variation in manganese content on the transformation structure are demonstrated.

INTRODUCTION

THE α solubility of manganese in titanium is small and reaches 0.5% at the eutectoid temperature of 1020° F. The region of interest for heat treating is the $\alpha + \beta$ field which widens with decreasing temperature from the transformation point of pure titanium to the eutectoid point at 20 wt. % Mn as shown in the partial phase diagram (Fig. 1). Variation of cooling rates and isothermal anneals affect shape and content of the α present and, therefore, the composition of the β phase, as well as the mechanical properties.

Rem-Cru Titanium, Inc., manufactures a binary titanium-manganese alloy under the trade name of RC-130A. In the present study,* the weldability of RC-130A alloy which was known to embrittle completely after welding is correlated to thermal history by means of microstructural examination. The reader is referred to the preceding study of the "Effects of Alloying Elements on the Weldability of Titanium Sheet"† for the description of the experimental approach, the welding procedure and testing methods used.

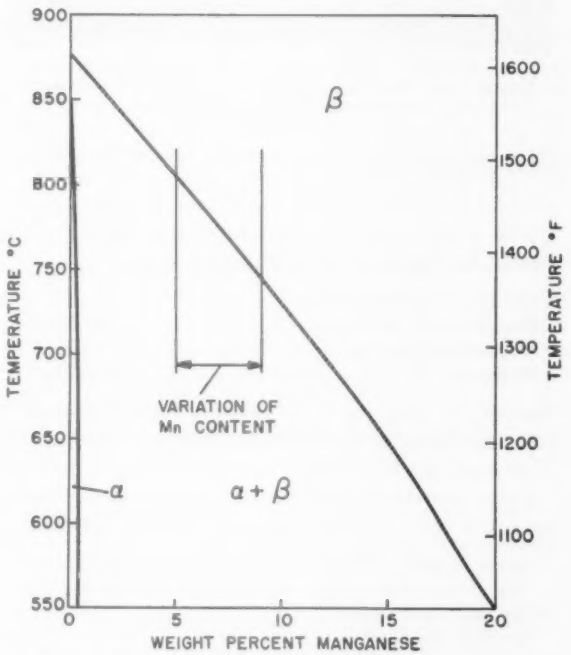


Fig. 1 Partial phase diagram of Ti-Mn system

THE UNWELDED AND THE AS-WELDED SHEET

Examination of the RC-130A sheet as-received indicated that it had been heat treated by the manufacturer after rolling to 1/16 in. thickness (see Fig. 2). From the absence of old β grain boundaries it may be deduced that the annealing temperature was below the

H. M. Meyer is Research Metallurgist, Armour Research Foundation of Illinois Institute of Technology, Chicago, Ill.

* WADC-TR-53-230.

† The Welding Journal, 33 (4), 173-s to 186-s (1954).

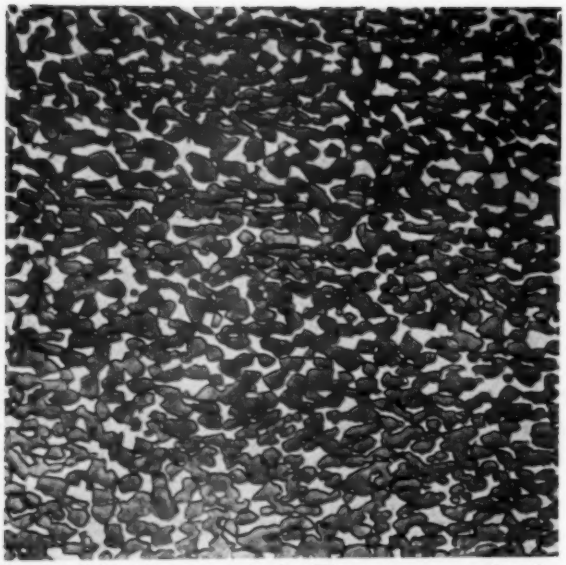


Fig. 2 RC-130A, as-received. The coarse coagulated α indicates that the manufacturer has given the rolled sheet a prolonged anneal at a temperature below the $\beta/\alpha + \beta$ boundary

Mechanical properties: 130,000 psi—UTS; 1/4 in.—folding distance; 17%—elongation. × 750.

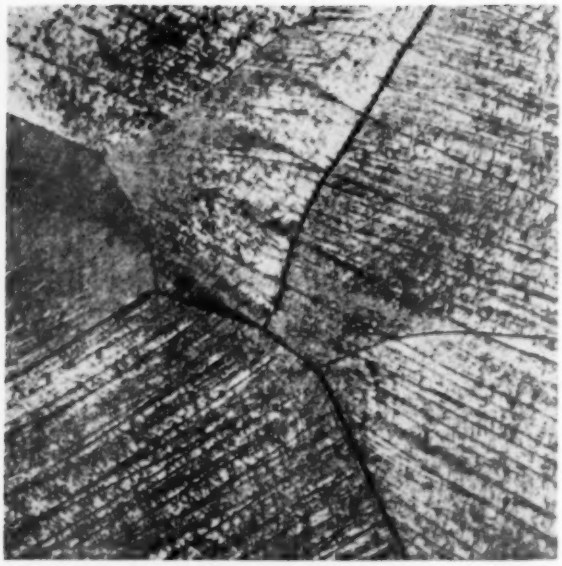


Fig. 3 Weld metal in the as-welded state shows a rejection of very fine α as a result of the cooling history after welding. The striations are of unknown nature. The structure is indicative of poor ductility

Mechanical properties: 65,500 psi—UTS; 5/8 in.—folding distance; 1%—elongation. × 750

$\beta/\alpha + \beta$ boundary. In this form the sheet is quite ductile, having the following properties:

Free bend folding distance	1/4 in.
Ultimate tensile strength	130,000 psi
Elongation at fracture	17%
Hardness	283 Vhn

Mechanical property tests in directions both parallel and perpendicular to the rolling direction disclosed no substantial directionality.

In the as-welded state, the bend and tensile specimens indicated the following seriously deficient properties:

Free bend folding distance	5/8 in. (fracture in weld metal)
Ultimate tensile strength	65,500 psi (fracture in weld metal)
Elongation at fracture	1%
Hardness (weld metal)	401 Vhn

The brittleness appears to be in the weld metal which, along with portions of the heat-affected zone, was cooled at a fairly fast rate from the all-β field. The microstructure of the weld metal in Fig. 3 discloses a fine dispersion of incipient α throughout the β grains. Examination of brittle titanium alloys has repeatedly pointed to the correlation of this type of structure with poor ductility. This configuration of α is characteristic of precipitates occurring at temperatures below 1030° F. The cooling rate of this weld was obviously such that sufficient time was spent in the temperature range 1030–750° F for α precipitation to begin.

Since the titanium-manganese system is of the eutectoid type, its alloys behave similarly to alloys of the titanium-chromium and titanium-iron systems. In all of them, the eutectoid transformations occur very sluggishly and can only be induced after protracted anneals. Therefore, the origin of heat treatability in practical alloys is the rejection of proeutectoid α from β. A broad range of compositions occurs in which β by quenching from its homogeneous field embrittles extremely by pre-precipitation. In the titanium-manganese system this range of composition includes 4 to 14% Mn. The RC-130A lies well within this range. As may be expected, sheet, water quenched from a β field temperature has little bend ductility.

THE POSTHEAT TREATED WELDMENT

Preceding experimentation suggested that the straight forward approach to recovering ductility was to re-solution treat and then to transform the β isothermally at some temperature at which sufficient α would be rejected in relatively coarse form.

The temperature for the solution treatment was 1560–1600° F which is 150° F above the $\beta/\alpha + \beta$ boundary for 6.5 wt. % Mn. The isothermal transformation temperature was 1335° F, that is, 100° F below the phase boundary. For comparison, in a number of cases, the solution treatment stage was omitted. Results are summarized in Table 1.

The microstructures of the weldments in RC-130A agree well with previous experience in other binary heat treated titanium alloys with an embrittling β range. Three structural characteristics are often associated with brittleness: (1) supercooled β affected by pre-precipitation; (2) finely dispersed α uniformly distributed throughout the β grains; and (3) coarse plate-like α confined to the grain boundaries. Coarse plate-like α uniformly distributed throughout can be expected to be associated with good ductilities provided that no pre-precipitation occurs in the enriched β phase. In general, the size of platelets and the inter-

Table 1—The Mechanical Properties of Weldments in RC-130A Alloy

Postheat treatment	*Free bend folding distance, in.*	*Bend failure**	*UTS psi*	*Tensile elong., %*	*Tensile failure**
	4.6% Mn				
1560° F—15 min → 1335° F—30 min	3/8	HA	138,000	10	BM
1560° F—15 min → 1335° F—90 min	9/16	HA	131,000	5	HA
1335° F—30 min	7/8	BM	137,000	5	WM
1335° F—60 min	1/2	HA	146,000	16	HA
	6.8% Mn				
1600° F— 5 min → 1335° F—60 min	1/4	HA	142,000	4.5	HA
1600° F—15 min → 1335° F—30 min	3/8	HA	143,000	4	WM
1600° F—15 min → 1335° F—60 min	7/16	HA	134,000	2	BM
	8.8% Mn				
1560° F—30 min → 1335° F—30 min	3 5/8	BM	149,000	0.5	HA
1560° F—30 min → 1335° F—60 min	3 3/8	BM	156,000	2	HA
1335° F—30 min	3/8	HA	156,000	1.5	HA
1335° F—60 min	1 15/16	WM	136,000	1	WM
	6.8% Mn				
Unwelded	1/4	...	130,000	17	...
As-welded	5 3/8	WM	65,500	1	WM

* BM = base metal, HA = heat-affected zone, WM = weld metal.

lamellar distance are inversely proportional to the degree of undercooling at which the transformation is conducted. That is, the lower the transformation temperature, the finer and more closely spaced are the α platelets.

Hardness plots of the as-welded (Curve C) and post heat treated (Curves A, B and D) states are shown in Fig. 4. It is evident that in the as-welded state, the weld and heat-affected zones are hardened by the transformations associated with the cooling rate. (The structure is shown in Fig. 3.) The fractures both in tension and bending occurred in the hardened weld metal. Furthermore, the post solution treatment and isothermal transformation at 1335° F reduces the hardness of all zones of the weldment to a uniform level of between 300 and 325 VHN (this structure is shown in Fig. 7).

The occasion was taken to heat treat weldments with and without protection from the atmosphere. The resultant bend ductilities are given in Table 2. The following significant points can be deduced:

1. Sheet specimens contaminate rapidly at 1350° F and are thereby embrittled.
2. Vacuum sealing gives superior protection but immersion in lead is nearly as satisfactory.

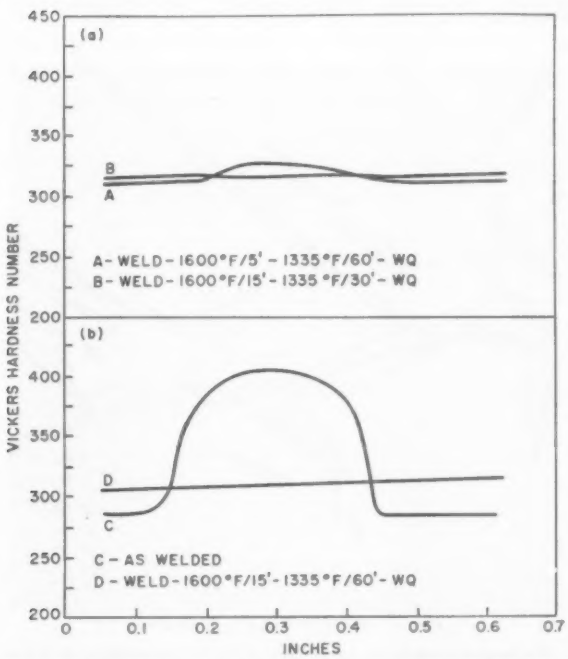


Fig. 4 (a) to (b): Hardness traverses of welded RC-130A sheet

Table 2—Effect of Heat-Treatment Atmosphere on Bend Ductility of Welded RC-130A Sheet

Postheat treatment Temp., °F	*Time, min*	*Protection*	*Free bend folding distance, in.*	*Tensile failure**
1350	5	None	5 1/4	WM
1350	30	None	5 9/32	WM
1350	30	Lead	2 11/16	WM
1350	30	Vacuum sealed	1 13/16	Cracks in WM
1350	120	Vacuum sealed	<1 3/4	None

* WM = weld metal.

A number of instances of solution treatment of RC-130A alloy in air at 1560° F for 60 minutes provided examples of another recognizable form of contamination. When the rate of infusion of oxygen, nitrogen or hydrogen is relatively slow, the concentrations do not build up at any point sufficiently to alter the all β structure. However, under conditions of contamination at more elevated temperatures this does happen and while the interior of the sheet is homogeneous β, the sub-surface is not. The changes occurring at the solution treatment temperature by virtue of rapid superficial infusion of contaminants easily can

be distinguished from α which is subsequently precipitated from β by a low temperature transformation. An example of the former condition is shown in Fig. 5.

SEGREGATION AND VARIATION OF COMPOSITION

The actual manganese content has not been constant. For the sheet stock required (of which there was sufficient material in only two batches to do appreciable experimental work), analyses by the Chemistry Department at the Armour Research Foundation reported the following contents:

Shipment	*Mn, wt. %*
A	8.0
B	6.8
C	4.6
D	8.8

This variation, which is indicated in Fig. 1, presents a problem to the user because prescribed heat treatments are often applicable only to particular compositions.

Therefore, the solution treatment followed by isothermal transformation at 1335° F yielded different

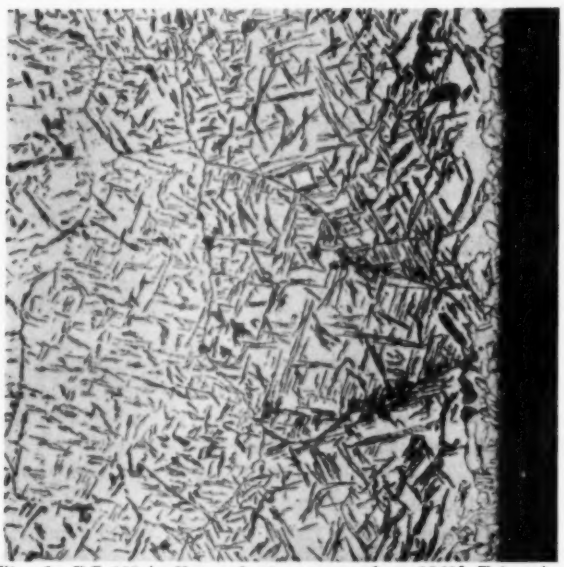


Fig. 5 RC-130A alloy solution treated at 1560° F in air. The subsurface shows contamination. ×150

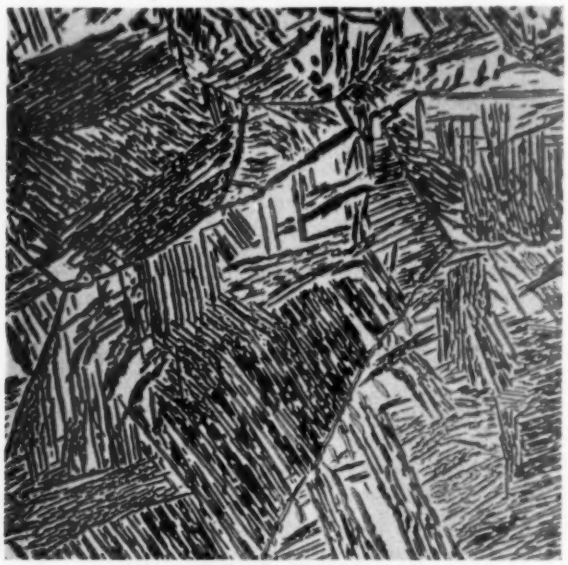


Fig. 7 RC-130A (6.8% Mn) alloy

Mechanical properties: 143,000 psi—UTS; 1/8 in.—folding distance; 4%—elongation. Heat-affected and weld metal in RC-130A weldments of varying composition. Postheat treatment: 1560° F—15 min→1335° F—30 min→WQ. ×250

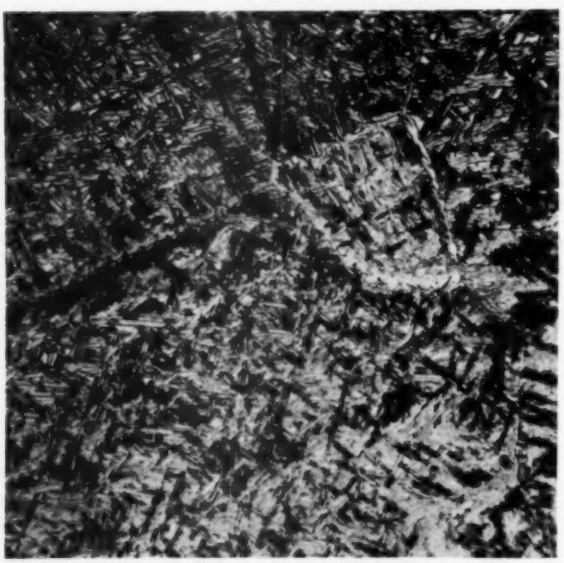


Fig. 6 RC-130A (4.6% Mn) alloy

Mechanical properties: 138,000 psi—UTS; 1/4 in.—folding distance; 10%—elongation. Heat-affected and weld metal in RC-130A weldments of varying composition. Postheat treatment: 1560° F—15 min → 1335° F—30 min→WQ. × 250.

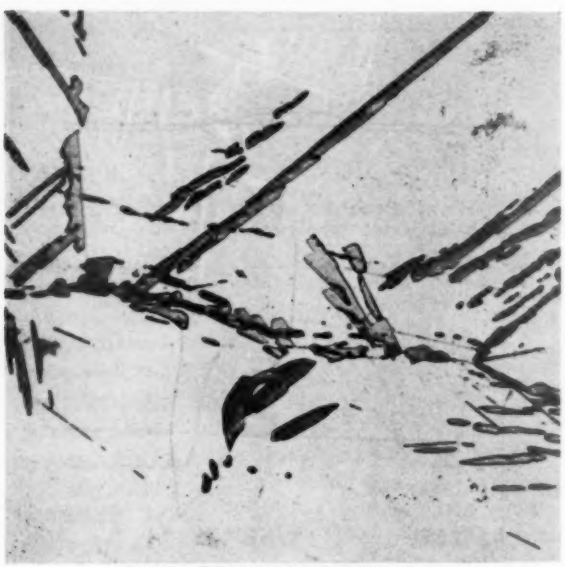
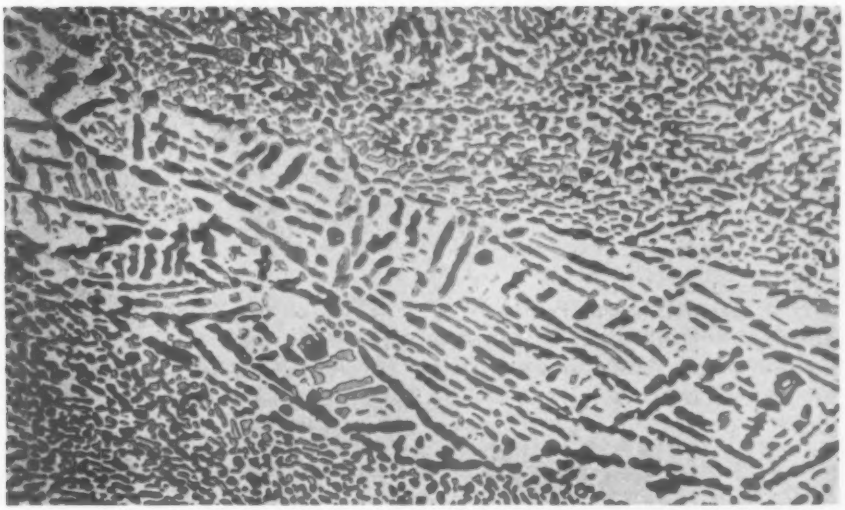


Fig. 8 RC-130A (8.8% Mn) alloy

Mechanical properties: 149,000 psi—UTS; 3/8 in.—folding distance; 0.5%—elongation. Heat-affected and weld metal in RC-130A weldments of varying composition. Postheat treatment: 1560° F—15 min+1335° F—30 min+WQ. ×250



Post heat treatment:
1335° F—30 min→WQ
Mechanical properties:
156,000 psi—UTS
$^{1}/_{8}$ in.—folding distance
1.5%—elongation. ×500

Fig. 9 RC-130A (8.8% Mn) alloy illustrating the appearance of pronounced banded segregation

structures according to the manganese content. With increasing content the 1335° F level is progressively closer to the $\beta/\alpha + \beta$ boundary and accordingly the α size should be modified progressively as discussed above. Figures 6, 7 and 8 represent microstructures produced by isothermal transformation of the 4.6, 6.8 and 8.8% Mn alloys, respectively. The increased coarsening of the α with increasing manganese content is evident. In the 8.8% Mn alloy, however, the α was confined to the β grain boundaries and the weldment was brittle and hard (397 Vhn). This serves to emphasize that no single heat treatment can satisfy a range of compositions. The lowering of the 1335° F temperature will probably alleviate the brittleness of the high alloy, although a low transformation level is undesirable for reasons mentioned above. Fortunately, it seems possible that a simple post ($\alpha + \beta$) anneal without a previous solution treatment avoids the embrittlement at least for the 30-min period; the corresponding structure is shown in Fig. 9.

This microstructure furthermore illustrates the variation of local alloy content because manganese had not dissolved homogeneously during the melting operation. The bend ductility was excellent. The segregation in bands parallel to the plane of the sheet notwithstanding, one may deduce that banding is not always deleterious.

SUMMARY

1. The bend ductility and tensile strength in weldments of RC-130A alloy sheet can be completely recovered by postheat treatment in protective atmosphere.

2. The postheat treatment consists of a solution anneal at 1560° to 1600° F from 5 to 30 min, followed by an isothermal transformation at 1335° F for 30, 60 or 90 min.

3. The 1335° F level, subsequent to a solution treatment, seems too high for the 8.8% Mn alloy.

4. Omission of the solution anneal stage gave satisfactory results with the 4.6% and—for the 30-min transformation—with the 8.8% Mn alloy.

5. Although failures occur commonly in the heat-affected zone, they may occur also in weld and base metals indicating uniform mechanical properties.

ACKNOWLEDGMENTS

The author wishes to express his appreciation to the Wright Air Development Center for permission to publish this paper.

Acknowledgment is also due to C. A. Johnson for his metallographic work, to Dr. J. M. Parks who was in charge of this study in its early stages and to Dr. W. Rostoker for his contribution to the understanding of the relationship between transformation structures and weldability.

30-Ft Diameter All-Welded Test Tank

Design and construction of a 30-ft diam all-welded steel tank together with a comparison of theoretical solutions to some of the problems and experimental test data taken during proof testing

by John Vasta and Frank W. Dunham

Abstract

Some unique problems encountered in the design and construction of a 30-ft diameter all-welded steel test tank are described. The theoretical solutions to some of these problems are given, and compared with experimental data taken during the proof testing of the tank.

The tank was designed to permit the testing of full-scale sections of submarine pressure hulls to collapse. The structure is approximately 74 ft in over-all length, and consists of a center cylindrical shell with two hemispherical heads. The relatively thin shell material, of 2-in. high yield strength steel, was designed for high internal working pressures considerably in excess of those permitted by current pressure vessel practice for this thickness-diameter ratio. New welding procedures were developed for the effective use of this material.

Some of the unique features of the design are: a system of "floating" internal stiffeners to support the dead weight of the otherwise completely unstiffened shell; the intersection of the small end of an internal cone with the crown of one of the hemispherical heads; the intersection of the large end of this cone with the cylindrical model.

The tank has been successfully proof tested, and is performing its function as a testing facility very well.

INTRODUCTION

THE design of a submarine pressure hull represents a problem in stiffened cylindrical shells subjected to external hydrostatic pressure. Uncertainties connected with the complex theory, coupled with the small margin of safety permitted in designs, has required that model tests be made to prove the adequacy of the design.

Until recently, these tests were made with models on the order of $^1/_{12}$ to $^1/_8$ scale. Several years ago it was decided that a facility for testing full-size sections of submarine hulls under external hydrostatic pressure would provide some very useful information on both the elastic and plastic behavior of the hull structure. To serve this purpose, therefore, a 30-ft diameter test tank was built by the Navy at Portsmouth Naval Shipyard.

The design of the tank was patterned after a 15-ft diameter test tank which had been used successfully for many years. However, the larger tank failed during its first test. This led to a re-examination of the problem resulting in the complete redesign of the tank and the incorporation of many unusual design features.

The welding procedures used in the construction of the tank were developed especially for the material, and represent one of the first attempts to develop the full efficiency of the joints in this material. The paper describes the welding procedures used, presents a description of the unusual features of design, and compares the experimental results with the theoretical values.

DESCRIPTION OF TEST TANK

The tank as finally reconstructed consists essentially of a 30-ft diameter cylinder about 45 ft long, with hemispherical heads at each end. The structure is supported by seven saddles under the cylindrical portion of the tank, Fig. 1, and rests on wood blocking inserted between the shell and the saddles, Fig. 2. The cylinder and the heads are unstiffened, but there are seven "floating" internal ring stiffeners, one in way of each saddle. These stiffeners are not welded to the tank shell, as they were designed solely to support the combined dead weight of the tank and the weight of water when the tank is filled without interfering with the expansion of the tank under pressure.

When it is desired to insert and withdraw submarine models from the tank, one end of the tank is cut entirely free at the butt joining the hemispherical head to the cylinder. A special cradle is provided to support this head when it is removed from the tank.

The internal arrangement of the tank is shown in Fig. 2. To provide access to the interior of the submarine model, a cone adapter was provided to join one of the hemispherical heads with a bulkhead to which the model is directly welded. A large forged toroidal ring, referred to as the "donut" ring, reinforces the crown of the hemispherical head at the small end of the

John Vasta is Head, Hull Scientific Section, and **Frank W. Dunham** is Supervisory Naval Architect, Design Division, U. S. Navy Department, Bureau of Ships, Washington, D. C.

Presented at the AWS National Spring Meeting held in Buffalo, N. Y., May 4–7, 1954. The views and statements in the paper are those of the authors and do not necessarily reflect the official position of the U. S. Navy Department. Manuscript received April 29, 1954.

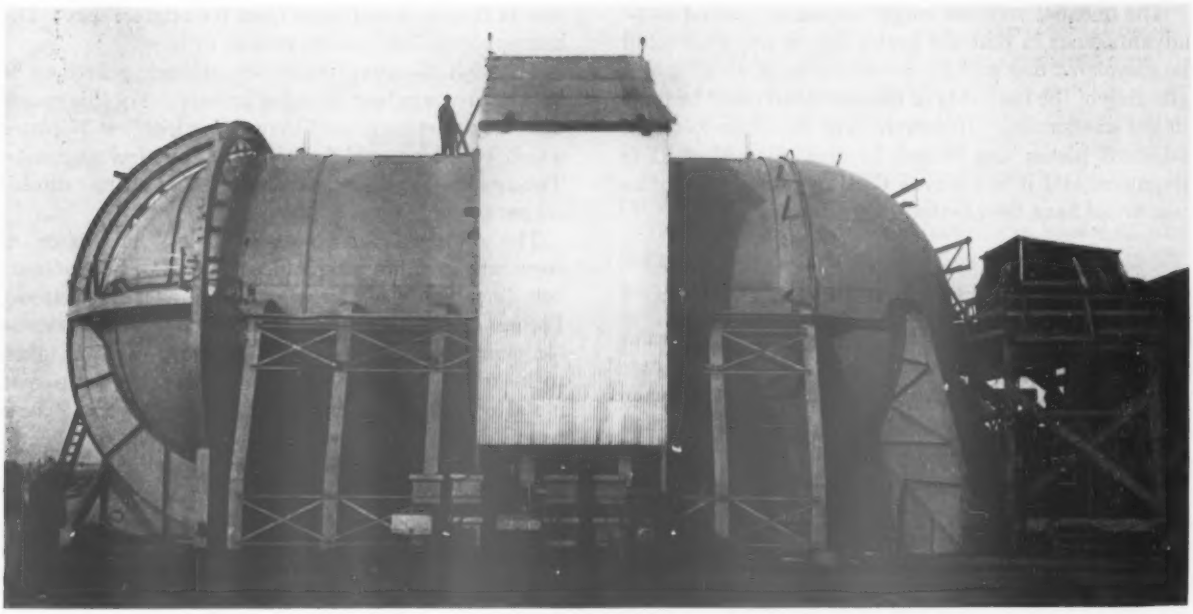


Fig 1. Side view of test tank

cone. The cone bulkhead has an opening to provide access to the model during the initial phases of a test.

The cylindrical shell and hemispherical heads are constructed of high strength steel 2 in. thick.

ORIGINAL TANK DESIGN

The tank was designed originally as a cylinder with flat end bulkheads, similar to a previous 15-ft tank which had proved successful. One of these bulkheads had a $7\frac{1}{2}$-ft opening for access into the model. Details of the flat end design are shown in Fig. 3. The tank was supported by ten saddles welded directly to the shell. In way of each saddle, a heavy external reinforcing ring was also welded around the upper half of the shell.

During its first test the tank failed at a pressure considerably below the designed working pressure. The cylindrical shell cracked near the juncture with one of the flat bulkheads as shown in Fig. 3.

FABRICATION OF ORIGINAL TANK

The cylindrical tank shell was fabricated from 7- by 28-ft plates rolled to a 15 ft radius. The long edges were beveled with an unequal $\frac{5}{8}$–$1\frac{3}{8}$-in. double-vee with the intention that, during erection, the deeper vees could be welded downhand from the outside-top and inside-bottom of the cylinder. The erection procedure was subsequently changed so that five of the 7- by 28-ft plates were joined into a 35- by 28-ft strake. The plate butts were welded in a rocking jig in the shop so that all the welding could be positioned downhand. The cylindrical portion of the tank thus consisted of 3 strakes each 28 ft in girth, and a closing strake of about 10-ft girth. End pieces were added to this 35-ft section, as shown in Fig. 3.

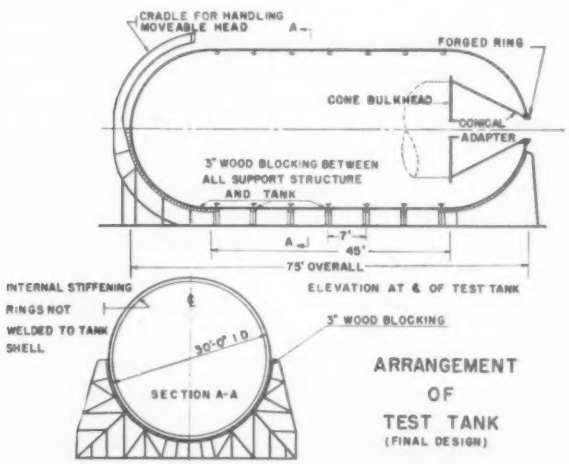


Figure 2

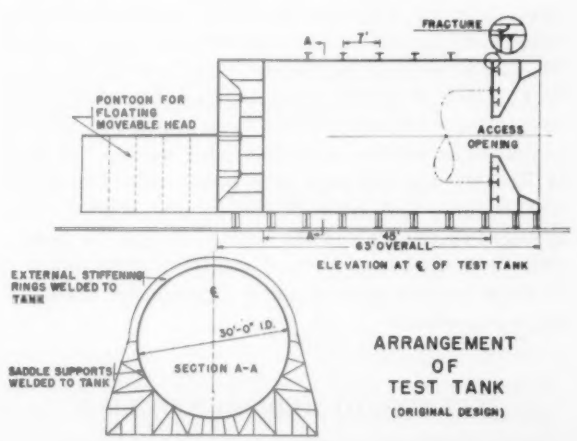


Figure 3

The unequal vees, no longer necessary, proved to be advantageous in that the heavy side of the weld could be completed first and the necessary flame-gouging and grinding of the back side of the root bead could be done in the smaller vee. However, some distortion between adjacent plates was caused by this unequal welding sequence, and it is believed that an equalized double-vee would have been better.

WELDING OF ORIGINAL TANK

The saddles to support the center cylinder of the tank provided a deep-web structure in the transverse center plane of each 7-ft cylinder section. The saddles were set up temporarily for erection purposes and modified by the insertion of nests of rollers in the faying-flange surfaces. These rollers were made in nests of five, each nest being carried on a trunnion secured to the saddle in such a way as to permit easy access to the surface of the plates.

The strakes were set into the saddles and supported by temporary internal framing. All seams were fitted and brought together to make a cylinder complete except for the welding of the four 35-ft seams.

The seams were welded while positioned at the horizontal about 1-ft above the saddle tops. The rollers in the saddles permitted rolling the entire cylinder to bring each seam into this position for welding.

The positioning of the cylinder seams was at first estimated to require two 40-ton cranes, but the application of one 9-ton chain fall served the purpose very nicely.

A back-step block sequence with blocks alternating on each side of the seam was used, the blocks being about 12 in. long. Flame-gouging and grinding of the backside of each block root consumed as much time as the actual deposition of weld metal. Carbon-arc gouging was tried, but was not too successful. A twin-arc technique and a few other experiments were tried in an attempt to save the root bead, but it was finally conceded that the root bead would inevitably crack, and would have to be sacrificed.

The initial specifications for using the welding electrode called for a preheat of 400° F maintained in the vicinity of the weld and a room temperature of at least 70° F. Subsequent laboratory tests showed that a preheat of 200° F would prove satisfactory. Preheaters were initially arranged along all four seams and all were heated while any one seam was being welded. A total of 70.4 kw was put into each seam with 12 long flat 4000-w units and eight of the tubular 2800-w units arranged on each side of the seam. During the August 1949 three-week heat wave, it was found that only half of these heaters were required to maintain a 200° F plate temperature.

WELDING OF REDESIGNED TANK

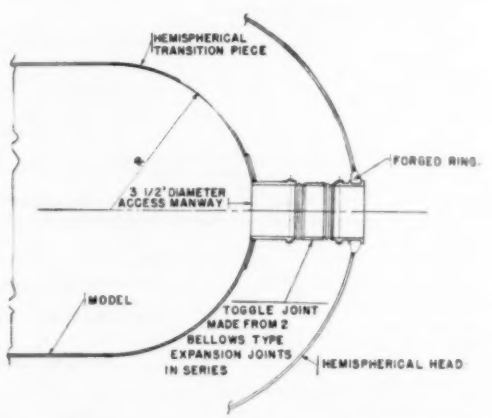
The length of the cylinder of the reconstructed tank was 44 ft 4 in., as salvaged from the original one. The hemispherical heads were welded to it.

The 30-ft diameter hemispherical heads proved to be too large to form by a spinning process. For this reason each head was fabricated by welding together 37 plates which had been cold formed into spherical segments. The material for the heads matched that of the cylindrical portion of structure.

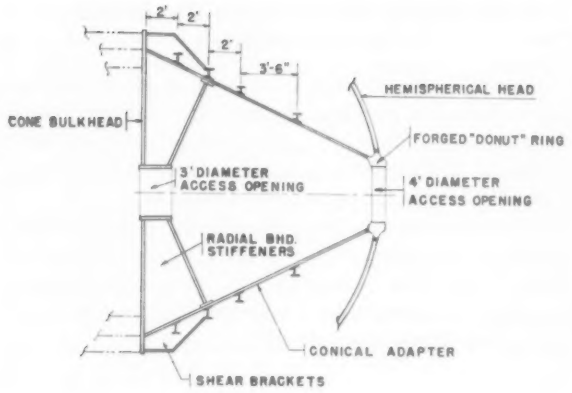
The welds are multiple-pass double-V butt joints incorporating military specification MIL-E-986 Grade 260 (low-hydrogen, ferritic, high-strength) electrode. Preheat of 150 to 200° F and maxium 300° F interpass temperatures were specified. Particular care was taken to insure that all deposits in contact with the parent metal consisted of a minimum of two layers. These were laid in such sequence as to temper the layer in contact with the parent metal by depositing a bead of the second layer over it before the temperature of the first bead dropped below the specified preheat temperature of 150° F.

REDESIGN OF TANK

The failure of the original design precipitated a series of investigations and proposals for repair. Several methods of repairing the tank, retaining the flat end bulkheads, were devised. However, it was decided to eliminate the possibility of further trouble at this critical juncture by discarding the flat end bulkheads, and replacing them with hemispherical heads. Since access to the interior of the model was considered desirable, this meant providing an opening in one of the hemispherical heads and a connecting passage. Two schemes were developed. One proposed the fitting of a cylinder with a bellows insert, Fig. 4, to connect the crown opening of the hemispherical head of the tank with a smaller hemispherical head welded to the submarine model. A bellows-type insert in the cylinder would restrict the loads acting on the "donut" ring to those caused by expansion of the head under pressure.


HEMISPHERICAL TRANSITION PIECE
FORGED RING
3 1/2' DIAMETER ACCESS MANWAY
MODEL
TOGGLE JOINT MADE FROM 2 BELLOWS TYPE EXPANSION JOINTS IN SERIES
HEMISPHERICAL HEAD

PROPOSED STRUCTURAL MODIFICATION OF TEST TANK.
Figure 4



ARRANGEMENT OF CONE

Figure 5

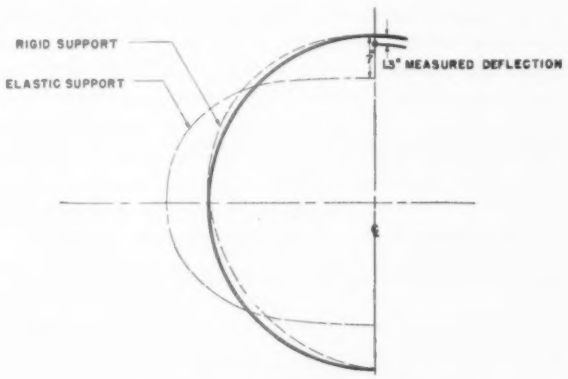
This scheme required a series of radial brackets around the model to transfer the unbalanced axial load to the tank shell. The second proposal, Fig. 5, which was ultimately accepted, provided a cone adapter which connected the hemispherical head of the tank to an inside flat bulkhead. This cone bulkhead served not only as the closure plate for the large end of the cone, but also enabled the submarine model to be welded directly to it. The chief drawback of the first proposal was the fact that a new hemispherical transition piece or some conical reducer would have to be built for each submarine model of different diameter. Moreover, there was insufficient experience with the construction of a 4-ft diameter cylindrical bellows subject to extremely high external pressure, and its reliability in service could not be guaranteed. The attachment of shear brackets to the tank shell was considered objectionable. Though theoretically desirable to have a bellows transition piece which would free the crown of the hemispherical head from axial and bending forces from the model, practical considerations favored the cone adapter idea.

One of the critical loadings on the test tank was that due to its combined dead weight and weight of water prior to pressurizing. The failure of the first tank directed attention to the restraining influence on the shell from the saddles and associated external rings. Thus, two conflicting requirements had to be met: provide adequate circumferential stiffening support in order to carry the dead weight of the tank and the weight of water; and permit freedom of movement to eliminate secondary stresses due to restraint. The problem was finally resolved by cutting loose the tank from the saddles, removing the upper external reinforcing rings in way of the saddles, and providing a system of "floating" internal rings, Fig. 2.

INTERNAL FLOATING STIFFENERS

The internal floating stiffeners were designed to sustain the dead weight loading of the shell and the water contained in the tank. One stiffener was placed at each transverse plane in way of the external saddle. The load carried by each stiffener was assumed equal to the load over one complete bay between saddles. The stiffeners were not welded to the shell. Longitudinal positioning of the floating rings was provided by a number of longitudinal stiffeners welded to the rings to form a floating structural cage.

The analysis of the ring frame was based on the elastic energy theory of Van Den Broek.[1] Since the support offered by the saddles could not be assessed exactly, because of the wood blocking inserted between the tank and saddles, two limiting conditions of support were assumed, rigid and elastic (see Appendix). The rigid support assumed a uniform radial force in way of the saddles, whereas the elastic support assumed a radial support varying in intensity from a maximum at the bottom to zero at the saddle top.



DEFLECTION CURVES FOR INTERNAL STIFFENING RINGS UNDER DEAD LOAD
TANK FULL OF WATER – NO PRESSURE

Figure 6

The calculated deflection curves for the two assumed supports are shown in Fig. 6. It is to be noted that the deflection at the top centerline obtained after filling the tank was 1.3 in., indicating that the saddles were offering almost rigid support to the tank.

Strains were also measured in the tank shell and the floating stiffeners during the filling operation. The experimental stress distribution shown in Fig. 7 again confirms the assumption that the saddles provide almost rigid supports. The stress in the tank shell arising from dead loads was quite small, not exceeding 5000 psi.

TANK SHELL

Under pressure, the cylindrical tank shell can be analyzed by the well-known simple hoop formula: $\sigma = pD/2t$. Many strain gages were placed on the tank shell to determine the behavior of this structure under pressure. Comparison of theoretical stresses with those observed is shown in Table 1 for a pressure of 100 psi. The experimental stresses are the average of many observations taken around the circumference

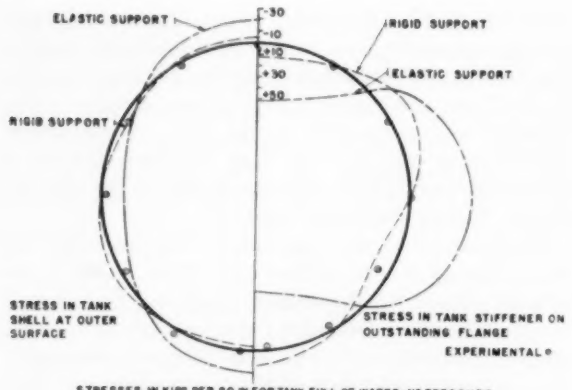


Figure 7

of the cylindrical shell. Both the circumferential and the longitudinal stresses compare well with the theoretical values.

Table 1

	Circumferential, psi	*Axial, psi*
Theoretical	+9,200	+4600
Experimental	+10,000	+5700

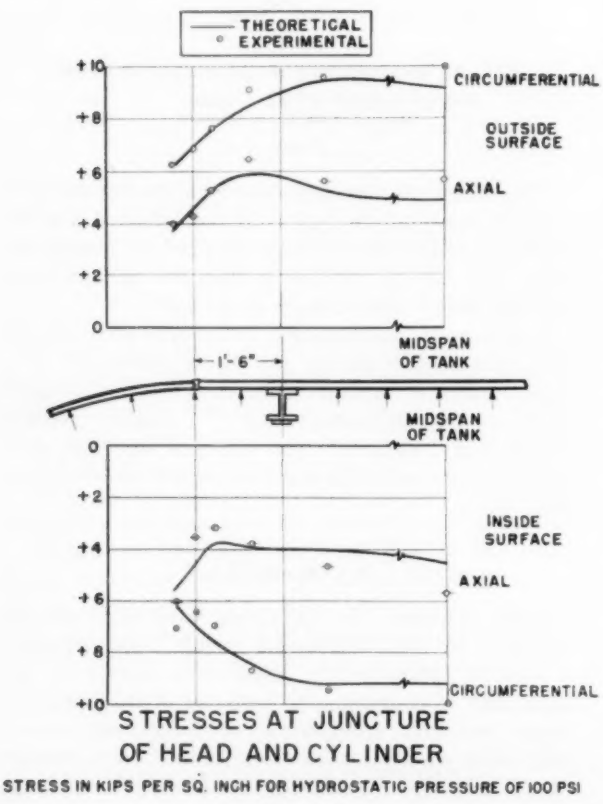


Figure 8

HEMISPHERICAL HEAD-CYLINDER INTERSECTION

Since the hemispherical heads were made as thick as the cylinder, no bending moment due to discontinuity arises at their juncture. However, a shearing force does exist, and its effect on the magnitude of the stresses in the vicinity of the joint was investigated.

The maximum axial and circumferential stresses were calculated to occur at 11.6 and 27 in. from the joint, respectively (see Appendix). Comparison between experimental stresses and theoretical values are shown in Fig. 8 and are discussed below.

TOROIDAL OR "DONUT" RING

The hemispherical heads eliminated the problem of the discontinuity stresses at the juncture with the cylindrical shell. However, in order to provide access to the model, an opening was required in one of the hemispherical heads, Fig. 5. This opening, in addition to enabling inspection of the model during the initial phases of the test, facilitated carrying the strain gage leads from the model to the instrument house. The reinforcement of the opening at the hemispherical head presented a unique design problem which required the development of a general analysis.[2]

The structural elements under study consisted of the hemispherical shell with a crown opening, a toroidal reinforcement ring and a conical adapter. The loading on these structural elements consisted of internal hydrostatic pressure acting on the hemispherical head, external pressure on the cone, and an unbalanced axial load proportional to the area of the crown opening. The "donut" ring is loaded by forces and moments resulting from the deformation of the intersecting structures under these loads.

Two possible secondary effects were also considered: tilting of the cone due to the rise of the end of the tank relative to the model when under pressure, and tilting of the submarine model due to buoyant forces. At full test pressure the stresses in the "donut" ring from the first effect were calculated[2] to be less than 7% of the total stress. The second effect was considered negligible, since the model undergoing test would be ballasted negatively to prevent its rising.

In the analytical study of the "donut" ring, four simplifications were made:

(*a*) The cone was assumed as an equivalent long, unstiffened cylinder with the same diameter as the "donut" ring.

(*b*) The reinforcing ring was assumed to have a rectangular cross section $5^1/_2 \times 10^1/_4$ in.

(*c*) The connection of the hemisphere to the tank cylinder was assumed to be remote enough to have no influence on the deformations of the opening.

(*d*) The axis of symmetry of all three structural components was assumed to coincide at all times.

The assumed rectangular cross section neglects the fillet material which was ultimately provided in the

structure to ease the transition from the hemispherical shell and cone to the ring. This simplification was believed to produce discontinuity shears and moments on the conservative side. Stresses at the intersections were calculated by considering the reinforcing ring with and without the fillet material to obtain the lower and upper bounds of the stress level. The fillet material was represented by an equivalent increase in thickness in the hemisphere and cone.

In determining the size of the ring, three important conditions were used as a guide:

(*a*) The loads to the ring were assumed transmitted from the cone and hemispherical head directly to the centroid of the ring.

(*b*) The cross-sectional area of the ring was made ample in order to limit the stresses to acceptable levels.

(*c*) The maximum stress in the shell structure adjacent to the juncture with the ring was to be reduced by a generous fillet transition piece gradually tapering from the ring proper to the thickness of the attached cone and hemispherical head.

Many configurations were studied, resulting in the toroidal ring finally selected.

CONICAL ADAPTER AND CYLINDER-CONE INTERSECTION

The plating for the conical adapter was designed as an equivalent cylinder of diameter equal to the large opening of the cone. The stiffeners were designed as rings supporting an equivalent band of cylinder plating having a diameter equal to the diameter of the cone at each ring section.

The final design, Fig. 5, consisted of a cone made of high-strength steel, $1^1/_2$ in. thick, 18 ft in diameter at the large end, 4 ft in diameter at the small end and approximately 14 ft long. The cone was stiffened by four external ring stiffeners welded normal to the axis of the cone. The three stiffeners near the large cone end were spaced 2 ft on centers. The fourth one, close to the smaller opening, was increased in spacing to $3^1/_2$ ft.

A flat stiffened bulkhead made of high-strength steel, 2 in. thick, was provided at the large end of the cone to serve as the closing boundary to which the submarine model could be welded, and also to permit the proof testing of the tank including the cone.

The original intent was to weld the submarine model directly to the cone. Analytical investigations of the stresses at the intersection of the conical adapter and the cylindrical shell of the submarine model indicated high discontinuity stresses in the order of 2 to 3 times the yield strength of the material. The only theory available[3] treated the intersection of an unstiffened cylinder with an unstiffened cone. Because of these uncertainties, the design was developed with the idea of keeping all stresses below the yield point of the material at the maximum test pressure. To achieve this objective, recourse was made to a series of small-scale structural model tests.

The initial studies[4] indicated that the conical adapter might fail by general instability rather than by yielding of the material. The model study therefore was specifically aimed at investigating the stability of the conical section, and determining the stiffener spacing needed to develop the required strength.

The test results of several small-scale models of unstiffened cylinder-cone intersections confirmed the high discontinuity stresses predicted by the theory of Wetterstrom. Moreover, comparison of measured strains in the small models with theoretical values indicated that the assumption of the conical shell as an "equivalent" cylinder for the purpose of investigating the discontinuity stress at the common juncture of these structural elements was adequate.

The presence of stiffening rings on certain small-scale models was found to have considerable effect, as would be expected, in reducing the circumferential strains in their vicinity. However, the rings seemed to have little effect on the longitudinal or meridional strains. These model tests also indicated a radial outward movement of the cylinder-cone intersection under hydrostatic pressure. This movement introduced high-tensile strains in the outer fibers in the immediate vicinity of the juncture, and strongly suggested the need for circumferential restraint in order to reduce this movement to acceptable limits.

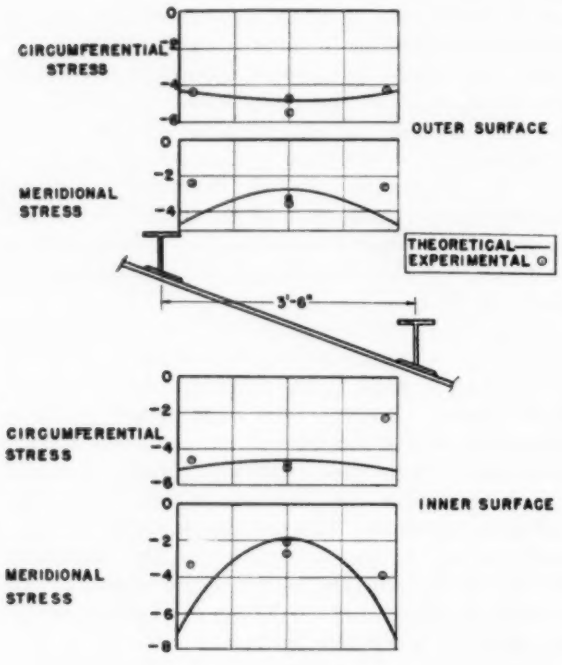
The effect of a reinforcement placed at the juncture of the cylinder and cone was investigated analytically and experimentally.[5]

For the analysis an approximate solution for the discontinuity stresses at a cylinder-cone intersection with a local reinforcement was developed. Comparison with the more rigorous solution of Dubois[6] was also made. It was found that the approximate solution gave results accurate enough for an engineering answer, the maximum error being 5%. The analysis indicated that, for the geometry of the test tank, the area of the reinforcement would have a much greater influence in reducing the stresses at the juncture than the moment of inertia. In consequence, the decision was made to provide a flat bulkhead at the large end of the cone, Fig. 5. This bulkhead not only reduces the stresses at the juncture of the cylindrical model with the cone adapter, but also simplifies the attachment of the submarine models and permits the proof-testing of the cone structure with no model in place.

COMPARISON BETWEEN THEORY AND EXPERIMENTS

The comparison between theoretical stresses and experimental values obtained on the test tank for a hydrostatic pressure of 100 psi is shown graphically in Figs. 8 through 12. Figure 8 shows the stress at the juncture of the hemispherical head with the cylinder. Figures 9 and 10 summarize the stresses for the cone and the cylinder-cone intersection, and Figs. 11 and 12 represent the stress distribution around the forged

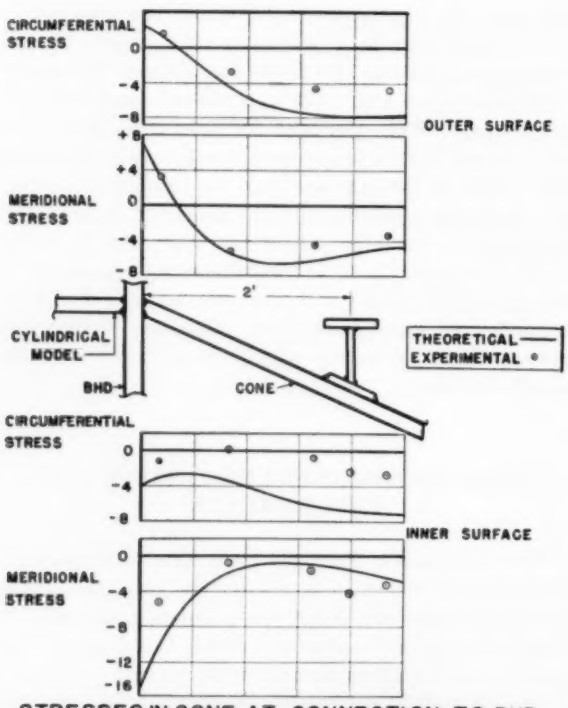
toroidal ring. The experimental points indicated in the graphs represent the average of observations taken



STRESSES IN CONE

STRESS IN KIPS PER SQ. INCH FOR HYDROSTATIC PRESSURE OF 100 PSI

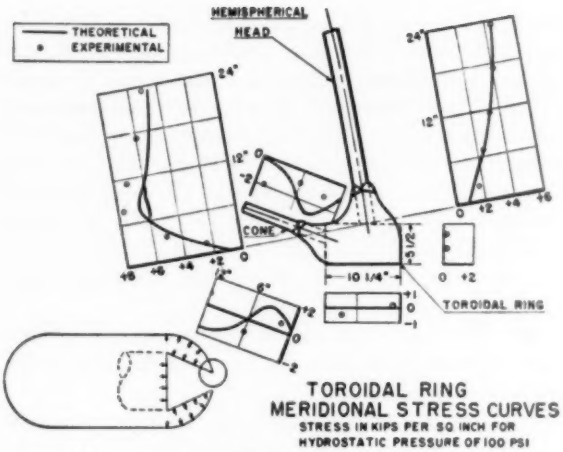
Figure 9



STRESSES IN CONE AT CONNECTION TO BHD.

STRESS IN KIPS PER SQ. INCH FOR HYDROSTATIC PRESSURE OF 100 PSI

Figure 10



TOROIDAL RING MERIDIONAL STRESS CURVES

STRESS IN KIPS PER SQ INCH FOR HYDROSTATIC PRESSURE OF 100 PSI

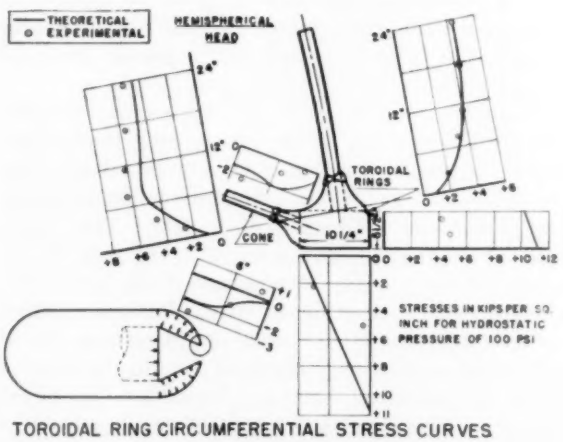
Figure 11

at selected points around the circumference of the structure.

The agreement between theory and test results for both the circumferential and axial stresses adjacent to the cylinder-head intersection is considered very good. The experimental stress distribution, Fig. 8, follows very closely the theoretical pattern. As can be seen, there are no large stress peaks at the juncture of these structural elements other than the slight stress increases adjacent to the joint as predicted by theory.

The experimental stress in the cone adapter, Fig. 9, shows good agreement with the theory for the region of the shell midway between stiffeners. Next to the stiffeners, however, there is some scatter but the experimental values are on the conservative side. The comparison is considered good when it is remembered that the theoretical stresses were obtained on the assumption that the cone could be treated as an equivalent stiffened cylinder of a diameter equal to that of the cone midway between frames.

The stress distribution near the cone-cylinder intersection, Fig. 10, follows the theoretical pattern remarkably well. Where deviation occurs, it is to be noted that the experimental stresses are again on the



TOROIDAL RING CIRCUMFERENTIAL STRESS CURVES

Figure 12

conservative side. The presence of the stiffener on the cone is evidenced by a dip in both the circumferential and meridional experimental stress values on the inner surface of the cone. The theoretical predictions do not include the effect of the stiffener.

The experimental stresses around the forged toroidal ring and the adjacent connecting structure, Figs. 11 and 12, show in most cases a favorable comparison with theory, especially for the distribution in the hemispherical shell. The measured stresses in the forged ring proper, however, are distinctly lower than those predicted. Undoubtedly the heavy fillets which provide the transition from the rectangular ring to both the cone and hemispherical head are beneficially influencing the intensities of the stresses in the forged ring.

CONCLUSION

The important conclusions from this study are:

(*a*) The behavior of the test tank with its unusual design features can be adequately estimated from theory.

(*b*) There are no dangerous stress peaks at the critical junctures of the various structural elements.

(*c*) Where differences between predictions and test results are noted the predicted stresses appear to be on the safe side.

(*d*) The combination of hemisphere-toroidal ring-cone adapter appears to work especially well for the purpose for which this structure was designed.

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Appendix

A. INTERNAL FLOATING STIFFENERS

The final expressions for the bending moment and axial forces obtained at all points along the plating[1] are as follows:

For rigid support:

$$M_{A-C} = \left(\frac{w_1 + w_5}{2\pi}\right) R(1.571 - \phi \sin\phi - {}^3/_2 \cos\phi) \quad (1)$$

$$M_{C-D} = \left(\frac{w_1 + w_5}{2\pi}\right) R(-1.571 - \phi \sin\phi - {}^3/_2 \cos\phi + \pi \sin\phi) \quad (2)$$

For an elastic support:

$$M_{A-C} = \left(\frac{w_1 + w_5}{2\pi}\right) R(1.274 - \phi \sin\phi - \cos\phi) \quad (3)$$

$$M_{C-D} = \left(\frac{w_1 + w_5}{2\pi}\right) R(1.274 + \phi \sin\phi + \cos\phi - \pi \sin\phi) \quad (4)$$

For axial force:

$$P = \left(\frac{w_1' + w_5'}{2\pi}\right)(2 - {}^3/_2 \cos\phi - \phi \sin\phi) + \left(\frac{w_1 + w_5}{2\pi}\right)({}^3/_2 \cos\phi - \phi \sin\phi) \quad (5)$$

Figure 13 represents graphically the two assumptions.

w_1 = weight of structure for 7-ft section.
w_5 = weight of water for 7-ft section.
w_1' = weight of structure for effective width.
w_5' = weight of water for effective width.
R = mean radius of tank.

Having determined the general expression for the bending moment, the deflection is computed by solving the differential equation

$$\frac{du^2}{d\phi^2} + u = -\frac{MR^2}{EI} \quad (6)$$

where

u = the deflection of the shell.
I = moment of inertia of the plate + moment of inertia of the stiffener.

The boundary conditions on the above differential equation for a rigid support are:

$$\left[\frac{du}{d\phi}\right]_{\phi=0} = 0; \qquad [u_1]_{\phi=(\pi/2)} = [u_2]_{\phi=(\pi/2)};$$

$$\left[\frac{du_1}{d\phi}\right]_{\phi=(\pi/2)} = \left[\frac{du_2}{d\phi}\right]_{\phi=(\pi/2)}; \qquad \left[\frac{du_2}{d\phi}\right]_{\phi=\pi} = 0; \qquad [u_2]_{\phi=\pi} = 0 \quad (7)$$

where

u_1 = deflection above horizontal centerline, $0 \leqslant \phi \leqslant \pi/2$.
u_2 = deflection below horizontal centerline, $\pi/2 \leqslant \phi \leqslant \pi$.

The boundary conditions for an elastic support are the same as for a rigid support, except that $[u_2]_{\phi=\pi} \neq 0$.

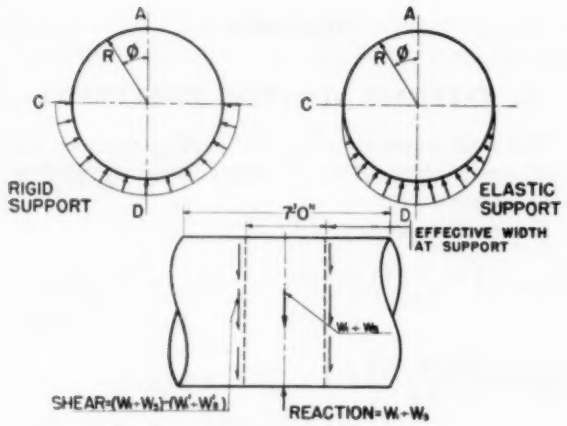


Figure 13

Bending stresses plus axial stresses in the frame and shell are obtained by

$$\sigma = \frac{M}{Z} + \frac{P}{A} \tag{8}$$

where $z=z_1$ is the section modulus for the frame to determine stresses in the outstanding flange, and $z=z_2$ is the section modulus for the shell to obtain the stresses in the outside fiber of the shell, Fig. 14.

B. HEMISPHERICAL HEAD—TANK-CYLINDER CONNECTION

For a cylinder and hemispherical head of such a relatively small thickness-to-diameter ratio, displacements due to membrane stresses necessitate consideration of the bending stresses near the intersection. At the juncture proper there is no bending moment due to discontinuity because of equal thicknesses of the two structural parts. A shearing force,[7] however, does exist which produces a deflection at the edge of the cylinder equal to $\delta/2$. The shear expression is given by

$$Q_0 = \frac{p}{8\beta} \tag{9}$$

where

$$\beta = \sqrt[4]{\frac{3(1-\nu^2)}{R^2 h^2}}.$$

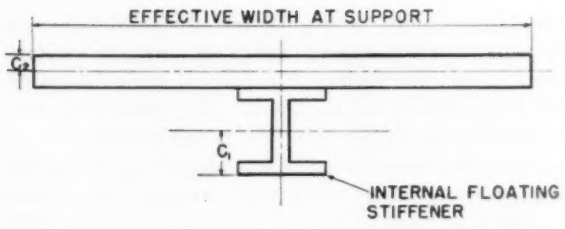
R = mean radius of tank.
h = shell thickness.

A unit strip of cylinder, Fig. 15, may now be considered as a long beam on an elastic foundation, bent by a force Q_0. The resultant bending moment in the axial direction at any point x from the joint is given by

$$M = -\frac{Q_0}{\beta} e^{-\beta x} \sin \beta x \tag{10}$$

with a maximum value at $\beta x = \pi/4$ or when $x = 11.6$ in. The axial stress σ is equal to

$$\frac{pR}{2h} \pm \frac{6M}{h^2}$$



$I_{TOTAL} = I_{PLATE} + I_{STIFFENER}$

Z_1 = SECTION MODULUS, INSIDE FIBER = I_{TOTAL} / C_1

Z_2 = SECTION MODULUS, OUTSIDE FIBER = I_{TOTAL} / C_2

Figure 14

The bending of the strip also produces a hoop stress made of the sum of two parts: (1) $\pm 6\nu M/h^2$ which prevents distortion of cross section of the strip, and (2) $-(Ey/R)$ which is due to the shortening of the circumference; y = inward deflection and E = modulus of elasticity.

Maximum circumferential stress is at $x = 27$ in. in the axial direction from the joint.

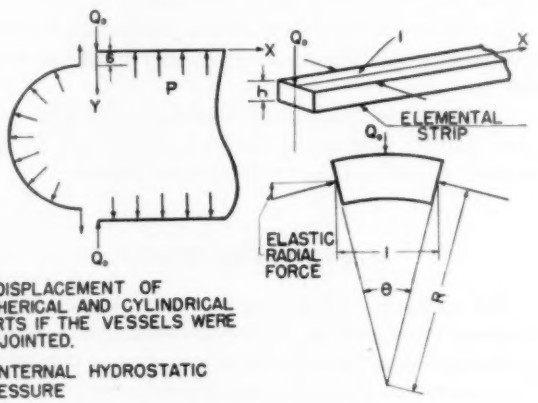


Figure 15

C. TOROIDAL RING[2]

Figure 16 gives the free body diagram of the structural elements analyzed. The stresses in the "equivalent" cylinder were determined from the following equations

$$N_0 = -\frac{pR_1}{2} \tag{11}$$

$$N_x = -pR_1 \tag{12}$$

$$M_x = 2D\beta^2 e^{-\beta x}\left[\left\{w_0 - \frac{pR_1^2}{Eh_c}\left(1-\frac{\nu}{2}\right)\right\} \times (\cos\beta x - \sin\beta x) + \frac{w_0'}{\beta}\cos\beta x\right] \tag{13}$$

$$Q_x = -2D\beta^3 e^{-\beta x}\left[2\left\{w_0 - \frac{pR_1^2}{Eh_c}\left(1-\frac{\nu}{2}\right)\right\} \times \cos\beta x + \frac{w_0'}{\beta}(\sin\beta x + \cos\beta x)\right] \tag{14}$$

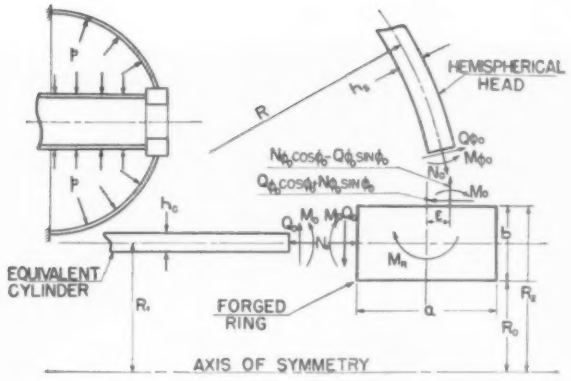


Figure 16

The stresses in the hemisphere were determined from the following equations

$$N_1 = \frac{pR}{2} \tag{15}$$

$$N_2 = -Q_\phi \cot \phi \tag{16}$$

$$M_\phi = -\frac{{h_s}^2}{12(1-\nu^2)R}\left[\frac{2\lambda^2}{\sqrt{\sin\phi}}\{C_3'C_{\lambda\phi} + C_4'A_{\lambda\phi}\} - \frac{\lambda\cot\phi}{\sqrt{\sin\phi}}\{C_3'B_{\lambda\phi} - C_4D_{\lambda\phi}\} + \lambda\left\{\frac{1-{}^1/_2\cos^2\phi}{2(\sin\phi)^{3/2}}\right\}\{C_3'A_{\lambda\phi} + C_4'C_{\lambda\phi}\} + \left\{\frac{\cos\phi({}^1/_2\cos^2\phi - 3)}{4(\sin\phi)^{3/2}}\right\}\{C_3D_{\lambda\phi} + C_4'B_{\lambda\phi}\}\right] \tag{17}$$

$$Q_\phi = \frac{1}{\sqrt{\sin\phi}}\left[e^{-\lambda\phi}(C_3'\cos\lambda\phi + C_4'\sin\lambda\phi)\right] \tag{18}$$

The stresses in the toroidal ring were determined from the following equations:

$$M_R = M_0 + M_{\phi_0}\left(\frac{R_2}{R_1}\right) - Q_0\frac{a}{2} - \frac{p}{6}\left(\frac{{R_2}^3}{R_1}\right) + \frac{p{R_1}^2}{6} - \frac{pR_2}{R_1}\left(\frac{a^2}{4} - \epsilon^2\right) + \left(\frac{Q_{\phi_0}}{\sin\phi_0} - \frac{pR}{2}\cos\phi_0\right)\epsilon\left(\frac{R_2}{R_1}\right) \tag{19}$$

$$\sigma_T{}^R = \frac{p_1{R_0}^2 - p_2{R_2}^2 + \frac{{R_0}^2{R_2}^2}{\rho^2}(p_1 - p_2)}{{R_2}^2 - {R_0}^2} \tag{20}$$

where:

$$p_1 = \frac{Q_0}{a}\left(\frac{R_1}{R_0}\right).$$

$$p_2 = \frac{1}{a}\left(\frac{Q_{\phi_0}}{\sin\phi_0} - \frac{pR}{2}\cos\phi_0\right) + \frac{p}{a}\left(\frac{a}{2} + \epsilon\right).$$

M_x = axial bending moment in cylindrical shell.
M_0 = axial bending moment in cylindrical shell at $x = 0$.
M_ϕ = meridional bending moment in spherical shell.
M_{ϕ_0} = meridional bending moment in spherical shell at $\phi = \phi_0$.
M_R = resultant twisting moment acting on toroidal ring.
Q_x = shearing force in cylindrical shell.
Q_0 = shearing force in cylindrical shell at $x = 0$.
Q_ϕ = meridional shearing force in spherical shell.
Q_{ϕ_0} = meridional shearing force in spherical shell at $\phi = \phi_0$.
N_0 = axial membrane force in cylindrical shell.
N_x = circumferential membrane force in cylindrical shell.
N_1 = membrane force in spherical shell.
N_2 = meridional force in spherical shell due to bending.
$\sigma_T{}^R$ = tangential stress in ring due to radial deformation.
p = hydrostatic pressure.
h_c = thickness of cylindrical shell.
h_s = thickness of spherical shell.
a = depth of ring.
ϵ = distance between median surface of hemisphere at juncture with toroidal ring and the centerline of ring.
x = axial coordinate along length of cylinder.
ϕ = angular coordinate along a meridional circle of the spherical shell measured from the axis.
ϕ_0 = half the central angle subtended by the crown opening of the hemisphere.
R_1 = radius to median surface of cylindrical shell.
R = radius to median surface of spherical shell
R_0 = radius to inside surface of toroidal ring.
R_2 = radius to outside surface of toroidal ring.
ρ = radius to any surface of toroidal ring.
w_0 = radial displacement of cylinder contiguous to ring.
w_0' = rotation of cylinder contiguous to ring.

$$D = \frac{Eh_c{}^3}{\sqrt{12(1-\nu^2)}}.$$

$$\beta = \sqrt[4]{\frac{3(1-\nu^2)}{R_1{h_c}^2}}.$$

$$\lambda = \sqrt[4]{3(1-\nu^2)\left(\frac{R_2}{h_c}\right)^2}.$$

C_3', C_4' = constants of integration for spherical shell.
$A_{\lambda\phi} = e^{-\lambda\phi}(\cos\lambda\phi + \sin\lambda\phi)$.
$B_{\lambda\phi} = e^{-\lambda\phi}\sin\lambda\phi$.
$C_{\lambda\phi} = e^{-\lambda\phi}(\cos\lambda\phi - \sin\lambda\phi)$.
$D_{\lambda\phi} = e^{-\lambda\phi}\cos\lambda\phi$.

D. INTERSECTION OF THE CONICAL ADAPTER AND CONE BULKHEAD

In terms of the edge forces H, edge moments M and pressure p, Fig. 17, the following approximate expressions were derived[5] for displacement perpendicular to a generator w, the meridional moment M_x and the transverse shear Q_x.

$$w = \frac{-\sqrt{2x}}{D\lambda^3}\frac{\eta}{\eta_0}\left\{\left[x_c H\cos\alpha + \frac{{x_0}^2\sin^2\alpha}{2}p + \sqrt{x_0}\,\lambda\left(\frac{2}{\xi_0} - \frac{\sqrt{2}}{2}\right)M\right]\cos\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right) + \frac{\sqrt{2}}{2}\sqrt{x_0}\,\lambda M\sin\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right)\right\} + \frac{3}{4hE}px^2\tan^2\alpha \tag{21}$$

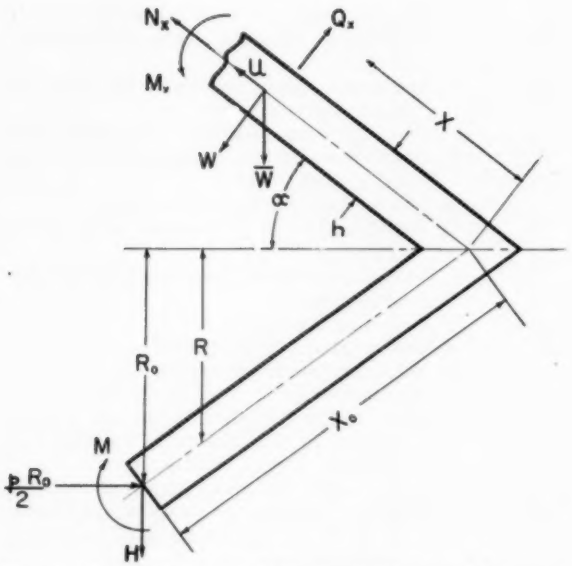


Fig. 17 Nomenclature and sign convention used for cones

$$M_x = -\frac{\eta}{\eta_0 \xi}\left\{\left[2\sqrt{2}\,x_0 H\cos\alpha + \sqrt{2}\,x_0^2\,p\sin^2\alpha - \sqrt{x_0}\,\lambda\left(2 - \frac{4\sqrt{2}}{\xi_0}\right)M\right]\sin\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right) - 2\sqrt{x_0}\,\lambda\,M\cos\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right)\right\} \quad (22)$$

$$Q_x = \frac{\eta}{\eta_0 \xi^2}\left\{[2\lambda^2(x_0^2 p\sin^2\alpha + 2x_0 H\cos\alpha]\times\left[\cos\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right) + \left(\frac{2\sqrt{2}}{\xi} - 1\right)\sin\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right) + 4\sqrt{2}\,\sqrt{x_0}\,\lambda^3 M\left[\left(1 - \frac{\sqrt{2}}{\xi_0} - \frac{\sqrt{2}}{\xi} + \frac{4}{\xi_0\xi}\right)\times\sin\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right) + \left(\frac{\sqrt{2}}{\xi_0} - \frac{\sqrt{2}}{\xi}\right)\cos\left(\frac{\xi_0 - \xi}{\sqrt{2}}\right)\right]\right\} \quad (23)$$

where:

$D = \dfrac{Eh^3}{12(1-\nu^2)}$.

$\lambda = \sqrt[4]{\dfrac{12(1-\nu^2)}{h^2\tan^2\alpha}}$.

$\eta = \dfrac{e^{\xi/\sqrt{2}}}{\sqrt{2}\,\pi\xi}$.

$\xi = 2\lambda\sqrt{x}$.

x = coordinate taken along the generator from the apex of the cone.

ν = Poisson's ratio.

h = thickness of the cone plating.

α = angle between the axis of the cone and the generator.

E = Young's modulus.

p = pressure acting on the outer surface of the cone

The meridional stress in the outer fiber of the shell may be simply computed from the following relationship:

$$\sigma_x = \frac{Q_x\tan\alpha}{h} - \frac{px}{2h}\tan\alpha \pm \frac{6M_x}{h^2} \quad (24)$$

The circumferential stress σ_ϕ may be similarly computed in terms of N_ϕ and M_x. However, a simpler approximation can be worked out from Hooke's law.

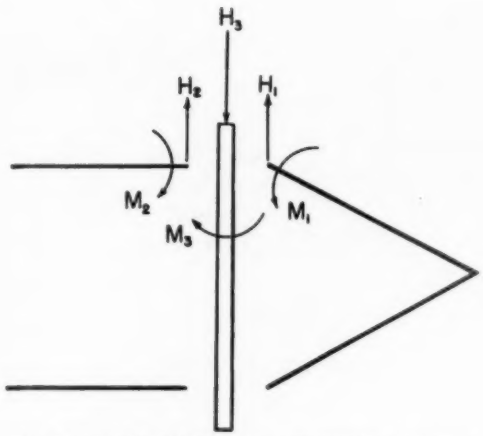


Fig. 18 Free-body sketch for intersection of cone and cylinder at a bulkhead

$$\sigma_\phi = -\frac{E\bar{w}_a}{R} + \nu\,\sigma_x \quad (25)$$

where:

R = radius to the median surface of the conical shell.

$\bar{w}_a$ = displacement perpendicular to the axis of the cone.

$$\bar{w}_a = w_b\cos\alpha + \frac{\left(1 - \frac{\nu}{2}\right)R^2 p}{Eh\cos\alpha} \quad (26)$$

and:

$$w_b = w - \frac{3}{4hE}\,px^2\tan^2\alpha \quad (27)$$

With the above expressions, the stresses at any point along the cone can be computed for any given H, M and p, Fig. 18. Since the cone is attached to contiguous structure, H and M represent the discontinuity shears and moments at the intersection and must be computed in terms of the geometry and elastic constants of the two intersecting structures.

In the case of the test tank conical adapter, there is a rigid bulkhead at the large end of the cone. This makes possible setting

$$w_1 = w_2 = 0$$
$$\theta_1 = \theta_2 = 0$$

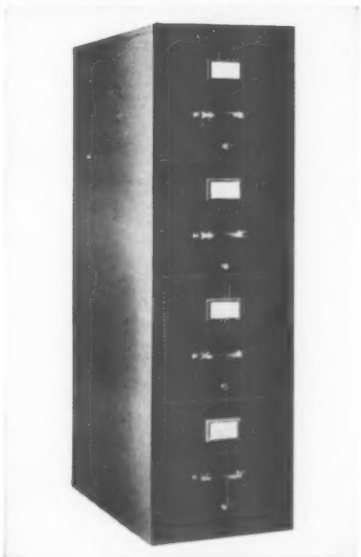
where θ is the angle of rotation of a generator due to deformation.

From these conditions, the following expressions for H_1 and M_1 can be derived.

$$H_1 = -\frac{R}{2}\tan\alpha p - \frac{3h\tan\alpha}{2\sqrt{12(1-\nu^2)\cos\alpha}}\,p + \left[\frac{1 - \nu/2}{\sqrt[4]{12(1-\nu^2)}}\sqrt{\frac{2hR}{\cos^3\alpha}}\right]p \quad (28)$$

$$M_1 = \frac{(1 - \nu/2)\,Rh}{\sqrt{12(1-\nu^2)\cos\alpha}}\,p - \left[\frac{\sqrt[4]{2}}{2[12(1-\nu^2)]^{3/4}}\sqrt{\frac{Rh^3\tan^2\alpha}{\cos\alpha}}\right]p \quad (29)$$

STRONG JOINT on desk housing is made with Tobin Bronze Welding Rod.



COLUMBIA Steel Office Equipment Co. also produces attractive filing cabinets like this one by braze-welding with Tobin Bronze Welding Rod.

Office equipment maker uses Tobin Bronze to braze-weld stronger, cleaner joints

On desks and filing cabinets it's important that all corner joints have adequate rigidity and strength, and that they look clean and neat after enamel finishing. Whether they will or not depends largely on the welds at these points.

Columbia Steel Office Equipment Co., Philadelphia, has this problem licked. First, they spot-weld all desks and filing cabinets. Then, units are braze-welded with Tobin Bronze* Welding Rods (3/32 in.). Columbia says, "Braze-welding with Tobin Bronze provides greater strength at the joints . . . helps to make our equipment more attractive. Our work goes faster, too. Tobin Bronze flows freely, 'tins' quickly, gives strong, neat bonds. Braze-welding with Tobin Bronze is the fastest, most economical way we've found to do the job."

ANACONDA Welding Rods for many types of production and repair jobs are available from distributors throughout the United States. *The American Brass Company, Waterbury 20, Conn.* In Canada: *Anaconda American Brass Ltd., New Toronto, Ont.*

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